DOE/DP/40006-1

FUSION DRIVER STUDY

MASTER

FINAL TECHNICAL REPORT
For the Period
April 1, 1978 — March 31, 1980

H.W. Friedman
April 1980

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H.W. Friedman

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FOREWORD

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ABSTRACT

A conceptual design of a multi-megajoule, repetitively pulsed CO₂ laser system for Inertial Confinement Fusion is presented. System configurations consisting of 50-100 kJ modules operating at subatmospheric pressures with multiple pass optical extraction appear feasible with present or near term technology. Overall laser system efficiencies of greater than 10% at repetition rates in excess of 10 Hz are possible with the state-of-the-art pulsed power technology.

A kinetics code description for the electrical power input and analytical model for the multiple pass optical extraction scheme specify the intrinsic efficiency of the laser medium. This combination of code and analytical model clarifies the impact of the intrinsic laser properties upon the total system design. The design of the Power Amplifier Module (PAM) is rectangular to reduce the pressure drops associated with a homogeneous laser gas flow. The dimensions of the PAM are determined by parasitic restrictions on the length, voltage standoff on the width and acoustic clearing time on the height. Medium homogeneity of the laser gas flow is shown to be sufficient for near diffraction limited output beams. Two designs for the pressure interface between the laser medium and the low pressure reactor chamber are considered, a large aperture, supersonic aerowindow and a mosaic of sodium chloride discs.

The constraints imposed on the system by pulsed power technology are examined. Large area, thermionic cathodes with magnetic guide fields to overcome the pinching and scattering limits are proposed. Designs for Pulse Forming Networks which are both directly and indirectly coupled to the load are considered and the question of the appropriate long lifetime switches is investigated. Those designs which allow pumping times of several microseconds and high load impedance are strongly favored from a pulsed power standpoint.

The design of the optical system is influenced by factors such as the diffraction quality of the beam, pellet spot size, optical, soft x-ray and neutron flux limits, coherency of the multiple beam lines, and the long optical path lengths required by the multiple pass extraction scheme. Several designs are considered for both aerowindow and salt window options.

The synthesis of all the laser subsystems into a specific configuration for a Laser Fusion Driver depends upon the reactor

chamber(s) layout, subsystem reliability and restrictions on overall dimensions of the fusion driver. A design is presented which stacks PAM in series in a large torus with centrally located reactor chamber. Cost estimates of the overall Laser Fusion Driver are also presented.

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I. INTRODUCTION AND EXECUTIVE SUMMARY

This document contains a conceptual design study for a CO₂ Laser Fusion Driver for electric power plant application. The primary objective of this study is to formulate a design concept which has efficiency and cost as a primary optimization parameter. A primary constraint on the conceptual design aside from that of technical feasibility is the extended lifetime of the subsystem components and associated technology which are required for practical operation of a power generation system.

The study was initiated with a critical assessment of the system inputs, required outputs and operating parameter ranges and an identification of the areas of technology which impact the total system. These areas include the gas flow for repetitively pulsed operation, aerodynamic and material windows for pressure interface, configurations for the Power Amplifier Modules (PAM), pulsed power technology, and the appropriate interfaces between the PAM, oscillator-preamplifier and target chamber. Included in this critical assessment is a forecast of the state of the art in these areas of critical technology for the 1980's time frame.

Technology tradeoffs and optimization of subsystem configurations were performed with regard to PAMs, isolation techniques, optics and beam transport, flow systems pressure interfaces and pulsed power systems. The scaling studies were traded off against capital cost, efficiency, lifetime and reliability. To aid in the tradeoff analysis and keep track of the interrelationships between the various subsystems, a computer code was developed. The code utilized selected design and cost algorithms for each subsystem so that, for the purposes of the code, subsystem optimization was already presumed. With the code, sensitivities of the cost and overall system efficiency could be easily computed to optimize parameters such as operating pressure, number of PAM, repetition rate, etc.

On the basis of the tradeoff studies and technology optimizations, a baseline conceptual design for a 1 MJ, 10 Hz CO₂ fusion driver was developed. The description of this baseline design together with a brief discussion of the subsystem rationale forms the basis of the following executive summary.

The output requirements for a CO₂ Fusion Driver are summarized in Figure 1 and reflect present estimates concerning pellet gains and power plant needd. The pulse energy of 1-5 MJ and repetition rate in the 10's of Hertz regime result in ouput electric powers in the 1000 MW range assuming pellet gains of the order of 100 and thermal to electical conversion efficiency of ~ 40%. The capital cost of such a laser system is less than several hundred million dollars per megajoule of energy on the target and the system has major component lifetimes in excess of 1 year (3 x 10⁸ shots at 10 Hz). An overall laser efficiency of > 10% is required in order to render the product

$$\eta G > 10$$

where η is the overall laser efficiency and G is the pellet gain.

A baseline design for a 1 MJ pulse, 10 Hz laser system is shown in Figure 2. Twenty large aperture PAMs supply the 1 MJ pulse energy with an output aperture of 3.2 m² per PAM corresponding to an aperture fluence of 1.75 J/cm², or 56 kJ/PAM. The gain length of the PAM is 2.6 m which corresponds to a one-pass, small-signal gain-length product of ~ 7. The high value of output fluence is achieved by multiple pass techniques with sufficient time delay between passes to insure efficient kinetic transfer of the CO₂-N₂ laser system. The operating pressure for the PAMs is 1/2 atm which (1) allows for a long electrical pumping time of 4 μ s and (2) eases the burden of the pressure interface between the laser system and the low pressure reactor chamber. A single aerodynamic window of 0.6 m² aperture is used as the pressure interface and all optical beams are passed through this window in a combination of angular and temporal multiplexing. The baseline design has also been configured with a material window option with an increase in cost and optical complexity but also an increase in overall efficiency. The overall efficiency and capital costs for the aerodyanmic and material window options are 10%, \$263 million and 11.4%, \$307 million, respectively.

One possible configuration for the 20 PAMs which has advantages for the laser gas flow is shown in Figure 3. The PAMs are arranged in a large diameter (~200 m) torus with an azimuthally directed flow. The optical axis is in the radial direction and the electrical axis (not indicated in the figure) is in the vertical direction. Radial tubes house the multiplex optics and extensions toward the center connect the final output beam for each PAM to the pressure window located at the center of the torus. The pressure drop across an individual PAM is sufficiently small to permit staging of two PAMs per compressor module.

REQUIREMENTS

- PULSE ENERGY 1 – 5 MJ
- PULSE DURATION 3 – 10 nsec
- REPETITION RATE 1 – 40 Hz

PRIME CONSIDERATIONS

- CAPITAL COST $\leq \$200 - 400 \text{ M / MJ}$
- LIFETIME AND RELIABILITY $10^8 - 10^9 \text{ SHOTS}$
- OVERALL EFFICIENCY $\geq 10\%$

J3661

Figure 1 Fusion Driver Requirements

ENERGY ON TARGET	1 MJ
PRF	10 Hz
POWER DURATION	5 nsec
NUMBER OF PAMS	20
NUMBER OF COMPRESSORS	10
ELECTRICAL PULSE WIDTH	4 μ sec
CAVITY PRESSURE	$\frac{1}{2}$ ATM
CAVITY DIMENSIONS	
GAIN LENGTH	2.6 meters
FLOW LENGTH	2 meters
DISCHARGE LENGTH	1.6 meters
PAM OUTPUT	56 KJOULES
APERTURE FLUENCE	2 JOULES/cm ²
AERO WINDOW APERTURE	0.6 m ²
INTRINSIC LASER EFFICIENCY	15%
OVERALL LASER EFFICIENCY	10%
LASER DRIVER DIAMETER	200 meters
TOTAL SYSTEM COST	\$263 MILLION

J1571

Figure 2 Baseline Design for Fusion Driver

J3682

Figure 3 Overall Layout for 1 MJ Fusion Driver System

-5-

A detailed rendition of this design concept is shown in the interior view of Figure 4. The gas flow enters the contraction section of the PAM where it is accelerated to a Mach number of ~ 0.2 . Electrical energy is added in the gain volume where lasing action takes place. The heated gas expands into the diffuser section where it is decelerated. A heat exchanger at the end of the diffuser removes the heat and the gas is injected into the next PAM. In this baseline design, the pressure drop per PAM is $\sim 5\%$ so that one compressor module per two PAMs is required to maintain a pressure uniformity of $< 10\%$.

The efficiency of the laser gas flow system is summarized in Figure 5. The number of dynamic flow heads, N_0 lost per PAM is of the order 3-5 for a well designed flow channel. The electrical input power to the PAM is the product of the total input energy and pulse-repetition rate where the relationship between repetition rate, flow Mach number and flow height is given by acoustic clearing rate. For a laser efficiency of $\sim 15\%$ and a flow Mach number of 0.1, the ratio of flow power to laser input power is $\sim 20\%$ and therefore flow is not a significant source of inefficiency for CO₂ Fusion Drivers.

An example of an efficient laser flow channel is shown in Figure 6. Laser gas is accelerated in the contraction section and enters the gain region where the e-beam sustained discharge adds energy to the flow. Approximately 15% of the input energy is extracted as laser radiation so that the bulk of the input energy must be removed before the next PAM. The hot gas expands and decelerates in the diffuser section. A heat exchanger is placed between PAMs to remove the waste heat and remove large-scale flow inhomogeneities and compressor modules are staged between the PAMs to power the flow. The lengths of the contraction and diffuser sections are expressed in terms of a contraction ratio, C , which is moderate Mach is ~ 3 . Thus for the baseline design where $H = 1.6$ m, the overall length of a PAM flow section is 20 m.

Scaling PAM apertures beyond the 10^3 cm² range requires additional control of the e-beam trajectories. For large values of the product of the discharge current density and laser aperture, the gyroradius of beam electrons in the self-magnetic field of the discharge becomes comparable to the discharge width and "pinching" occurs. The resultant nonuniform energy loading leads to the inefficient energy extraction and poor beam quality. A solution to this pinching problem is the application of a guide magnetic field parallel to the e-beam propagation. For the baseline design, a kilogauss field is sufficient to overcome the self field of the discharge current and is easily implemented with field coils. In Figure 7, a field coil structure is shown on an excimer laser.

Figure 4 Interior View of Fusion Driver Power Amplifier and Compressor Module

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- FLOW POWER

$$N_Q \frac{1}{2} \rho V^3 A = N_Q \frac{\gamma}{2} M^2 P_A$$

- LASER INPUT POWER

$$\frac{J}{\ell} \frac{WHL}{\eta} \text{ PRF}$$

- ACOUSTIC CLEARING TIME

$$\text{PRF} = \frac{a}{W} \frac{M(1-M)}{F}$$

- FLOW / INPUT POWER

$$\eta N_Q \frac{P}{J/\ell} \frac{M}{1-M} \cong \boxed{20\%}$$

J369I

Figure 5 Efficiency of the Laser Gas Flow System

J1444

Figure 6 High Efficiency Flow Tunnel Concept

Figure 7 Field Coil Structure on a Visible Laser

The pulsed power conditioning equipment must be reliable and long lived for power plant applications with components such as capacitors, switches, transformers, etc., surviving a minimum of 10^8 pulses. These requirements are major factors in the conceptual design of the laser system. For efficient transfer of electrical energy to the gas discharge, a pulse forming network is used and is coupled to the discharge load either directly or through a step-up pulse transformer. In the direct coupled case, the charge voltage on the PFN is twice the delivered voltage. The maximum component voltage is limited by breakdown considerations to the order of 100 kV and, hence, many switches must be stacked in series as well as parallel to implement the direct coupled design.

Near term projections of suitable spark gap switches indicate a total charge transfer lifetime of 50,000-1000,000 Coulombs per switch. This estimate results in the cost projections shown in Figure 8. For long electrical pump times, step-up pulse transformers can be used, thereby reducing the switch voltage requirements to within thyatron range. Near term projections of transformer technology indicate pulse duration as low as $\sim 4\mu s$ may be feasible. Below $\sim 4\mu s$, direct coupled PFN with spark gap type switches must be used giving significantly increased costs depending upon the switch replacement time. Recent developments in thyatron technology show operating voltages of $\sim 1/4$ MV, average powers of ~ 1 MW and lifetimes of $\sim 10^8$ shots and at this point thyatrons with step-up pulse transformers is the preferred choice for a pulsed power system.

The impact of the long pump times upon the system design is illustrated in Figure 9 where the electrical pump time is plotted versus the PAM pressure for constant values of laser efficiency. Below $4\mu s$, the pulsed power becomes difficult and above 1 atm, the power invested in flow related components such as aerodynamic windows becomes excessive. As the laser efficiency increases, both the input energy loading and small signal gain both decrease. The system tradeoffs are between high laser efficiency, high energy loading (i.e., high volumetric efficiency) and low value of g_0 to reduce the onset of parasitic oscillation. The baseline design of $1/2$ atm represents a compromise among these factors.

An important part of the conceptual design of Fusion Drivers is the multiplex concept which increases the output energy and efficiency of the laser. CO_2 lasers are efficiently operated by pumping nitrogen in a discharge and then collisionally transferring the vibrationally stored energy to the upper lasing level of CO_2 . The time scale for such vibrational transfer is hundreds of a nanoseconds (at one atmosphere) so that a single nanosecond pulse cannot fully drain

J1531

Figure 8 Cost Projections for Switchgear

J3701

Figure 9 Operating Parameters for Long Electrical Pumping Times

the nitrogen reservoir. Optical multiplexing involves the successive passage of beams through the laser medium with sufficient time delays to allow N_2 - CO_2 vibrational transfer. An example of the increase in efficiency of 5 pulses over that obtained by one pulse is shown in the calculation of Figure 10. Although the distribution of pulse energies depends upon the timing of the pulses, the total energy content extracted is roughly the same.

There are several schemes which can be used to multiplex the output energy. For conceptual designs where aerodynamic windows are used, the concept shown in Figure 11 is superior. One pulse passes through the PAM with appropriate time delays which are set by the distances between mirrors. The multiplex structures shown in Figure 4 house the beam-control optics and provide a controlled atmosphere for the beams to prevent loss of power due to absorption and refractive index gradients.

The PAM and the multiplex tubes are all at the same pressure (although the gas composition may be different), and hence at some point between the multiplex tubes and the target chamber, a pressure interface must exist. In the baseline conceptual design, the pressure interface has been placed at the center of the torus and is common to all the PAMs of the system. An aerodynamic window has been chosen as the primary candidate because of its long life, damage resistance, high fluence capability and good optical quality. The operation of an aerodynamic window is shown schematically in Figure 12. Gas enters the plenum, accelerates to sonic speed at the throat of the nozzle and expands supersonically into the PAM aperture. The supersonic flow expands into the exhaust duct and the centrifugal force associated with the turning flow balances the pressure gradient. Traditional designs for aerodynamic windows call for the high Mach numbers to minimize the storage of consumable gases and system weights. In a closed-cycle application, the power necessary to recirculate the gas is very high at high Mach numbers and this design is inappropriate. Aerodynamic windows have been reoptimized to minimize power consumption and the resultant design calls for low Mach numbers and multiple stages to achieve the total pressure standoff.

With this optimization, the power for an aerodynamic window is $15\text{--}30\text{ MW/m}^2$ at one atmosphere and the aperture must be minimized to maintain high overall efficiency. A multiplex concept shown in Figure 13 combines PAM output beams at the mirror fluence level of $\sim 10\text{ J/cm}^2$, into bundles at the air breakdown limit of $\sim 40\text{ J/cm}^2$. The bundles are then passed through the aerodynamic window at staggered intervals. Optics on the vacuum side separate the beams and remove the time delay.

Figure 10 Effect of Multiplexing on Energy Extraction

Figure 11 Single Pulse Multiplex System

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Figure 12 Operation of a Supersonic Aerodynamic Window

J1511

Figure 13 Multiplex Concept for Aerodynamic Windows

An alternate design concept using material windows is shown in Figure 14. As a result of the low fluence capability and limited diameter of suitable materials, i.e., $\sim 1 \text{ J/cm}^2$ and 60 cm diameter for NaCl, the PAM output is expanded and passed through a mosaic of material windows into the vacuum region. The beam is recollimated to the mirror limit and focused onto the pellet. Problems may arise with focusability of the beam due to loss of phase coherency of the beamlets within the beams and phase control may be required with this option.

The baseline design concept presented thus far accounts for the Power Amplifier Modules system exclusive of the Oscillator-Preamplifier system which drives it. In the calculations for the baseline design, it was assumed that the PAMs were operated in the fully saturated regime for maximum efficiency. The total gain of the amplifiers is ~ 14 so that for a 1 MJ output 70 kJ are needed to drive the PAMs. At 56 kJ/PAM, 2 PAMs would be required. The twenty PAMs in the baseline design include 2 for preamplifiers.

With the multiplex arrangement of multiple passes of a single beam, the first pass could be small signal, i.e., not saturating, thereby lowering the preamplifier requirements. However, the maximum gain coefficient in the PAM for a given number of passes is limited by parasitic considerations so that a tradeoff exists between preamplifier requirements and maximum gain. A lower maximum gain results in a larger number of PAMs. For the baseline system, the tradeoff favored fully saturated PAMs and two extra PAMs dedicated for preamplification.

The oscillator system must provide the short pulse with the correct spectral content for efficient energy extraction. For the baseline design at 1/2 atm and 5 ns pulsewidth 2-3 rotational lines per vibrational band are required for high extraction efficiency. Two vibrational bands are required for high efficiency making the total number of lines 4-6.

The overall efficiency of the baseline Fusion Driver is 10.4% starting from an intrinsic laser efficiency of 18.7%. A breakdown of the various subsystem efficiencies is presented in Figure 15. The salt window option adds about 1% to the overall efficiency but also increases the cost by $\sim \$44$ million.

In the remainder of this report, the design issues involved in the subsystems of the Laser Fusion Driver are discussed in detail and the relevant tradeoffs are presented. Critical areas of technology are evaluated and near term projections are made. In the final section, a computer model for economic predictions and efficiency calculations is presented.

J1577

Figure 14 Mosaic Concept for Salt Window Option

EFFICIENCY BUDGET

INTRINSIC LASER EFFICIENCY	18.7%
PULSED POWER SUPPLY — 85%	15.9%
BEAM TRAIN — 90%	14.3%
PAM OBSCURATION — 90%	12.9%
FLOW POWER — 90%	11.6%
AERODYNAMIC WINDOW — 90%	10.4%

J3698

Figure 15 Efficiency Breakdown for Baseline Design

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II. TARGET - OPTICS INTERFACE

A. INTRODUCTION

The interface between the pellet and the final optical element provides both guidance and limitations on the conceptual designs for the Laser Fusion Driver. The combination of optical and neutron fluence loadings which can be accommodated by the final mirror for long life operation together with the spot size on the pellet and area coverage of the reactor chamber limit the number of independent beams. Variations in focal spot movement and the accuracy of the pellet trajectory determine pointing and tracking specifications. These issues are examined in terms of current and near future state-of-the-art technology.

B. NUMBER OF INDEPENDENT BEAMS

For a set of final focusing mirrors located a distance, R , from the pellet as shown in Figure 16 the constraints arise from the spot size and the optical and neutron fluence levels on the final optic. A collimated beam enters the target chamber and impinges upon the final focusing element. For simplicity a square beam is assumed with the diameter of the final optic and the distance to the pellet given by D and R , respectively. The beam shown is one out of a total of N and it is further assumed that all N beams are independent, i.e., there is no phase coherence between the N beams. The spot size of the focused beam is given by

$$\delta \cong 2\sqrt{2} R(\lambda/D) (XDL)$$

where λ is the wavelength of CO_2 laser radiation and XDL is the ratio of actual spot size to the diffraction limited spot, i.e., XDL is a measure of the beam quality. The optical fluence on the focusing mirror is

$$J_M = \frac{E_T}{ND^2/2}$$

where E_T is the total laser energy on target. The neutron flux released by the thermonuclear burn results in a neutron fluence on the mirror given by

J4586

Figure 16 Geometry for Final Focusing Mirror

$$J_N = \frac{E_T G \eta_N}{4 \pi R^2}$$

where G is the pellet gain and η_N is the neutron production efficiency. Finally, the percentage of the reactor chamber wall covered by optics is

$$\% \text{ coverage} = \frac{ND^2}{4 \pi R^2}$$

where the mirror area has been increased by a factor of two to account for the hole in the target chamber through which the beam must enter. One can think of more complex schemes whereby focused beams pass through small holes in the chamber wall but such factors do not alter the basic conclusions of the section.

There are many uncertain factors present in this discussion including the relationships between pellet size, distance to the pellet, gain and energy on target. Also the maximum optical and neutron fluences which are acceptable commensurate with a long lifetime are not well known at this time. Optical damage threshold of 10 J/cm² for nanosecond pulsed on diamond turned copper surfaces has been measured but little or no damage data exists for neutron fluence. Two mechanisms have been identified for damage of bare metal optical surfaces due to neutron fluence.⁽¹⁾ The first is an increase in the optical absorption as a result of a reduction in the electrical conductivity in a lattice structure which has been damaged by neutron bombardment. Estimates of the total neutron fluence for this effect are 10¹⁹-10²⁰ n/cm² which corresponds to an energy fluence of

$$J_M \cong 0.07 - 0.7 \text{ J/cm}^2/\text{shot}$$

assuming a lifetime of one year at 10 Hz.

The second damage mechanism is the increase in optical distortion as a result of swelling from (n, α) helium production reactions. No experimental data on the effect of gas bubble production on the surface distortion of optical surfaces exists but there is data on the swelling rates of neutron irradiated materials. This data indicates that at a mirror-to-pellet distance of 50 m and an incident (steady) neutron fluence of 2.2 x 10⁻² MW/m², a surface swelling of $\lambda/8$ ($\lambda = 1.06 \mu$) occurs in a time of 1.3 - 4.3 years. For CO₂ lasers, the maximum total flux required for a 1/8 wave distortion is a factor of 10 higher due to the longer wavelength and corresponds to a neutron fluence of

$$J_M \approx 2.9 - 9.6 \text{ J/cm}^2/\text{shot}$$

again for a lifetime of one year at 10 Hz.

The ratio of neutron to optical fluence for one-year lifetime at 10 Hz ranges from

$$10^{-2} < J_N/J_M < 10$$

depending upon the damage mechanism. The number of independent beams vs the neutron pellet gain is plotted in Figure 17 as a function of the ratio J_N/J_M for a 1 mm pellet, beam quality of 1.5 times diffraction limited and component lifetime of one year at 10 Hz.

Another useful limit is the percent coverage limit where the quantity D/R has been replaced by the spot size relation, i.e.,

$$\% \text{ coverage} = \frac{2N}{\pi} \left(\frac{\lambda XDL}{\delta} \right)^2$$

This limit is plotted for 1 and 10% coverage with $\delta = 1 \text{ mm}$ and $XDL = 1.5$. The third limit of interest is the spot size relation recast in terms of laser energy on target and pellet gain. Combining the spot size relation with the optical fluence limit, the following relation is obtained

$$E_T = \frac{4R^2 \lambda^2 XDL^2 J_M}{\delta^2}$$

In order to plot this limit on the map, the relationship between the laser energy on target and the pellet gain is needed. Estimates supplied by Los Alamos indicate that within the range 1-5 MJ an average value of gain is

$$G \approx E_T/10^4$$

where E_T is expressed in joules. A recent EPRI report(2) gives a more pessimistic value in the same range of a factor of

Figure 17 Optical and Neutron Constraints on Final Focusing Mirror and the Number of Independent Beams

10 lower. Both these gain estimates are plotted on Figure 17 as the spot size limit. This limit is not completely specified in that the pellet size and distance to the pellet are also a function of the laser energy on target but their variation with energy is probably not great.

The result of this mapping exercise is to show the limits on the number of independent beams as a function of pellet gain. For pellet gains in the range 100-1000 and an optimistic estimate of the pellet gain variation with laser energy, the limiting factor for the number of independent beams is the neutron fluence. Worst case estimates, i.e., $J_N/J_M = 0.01$ predict focusing problems for small pellets while best case estimates, $J_N/J_M = 1.0$ will allow several tens of beams with no difficulty. If the pessimistic gain estimate is used with the spot size limit, then even for best case neutron fluence loadings, only a few tens of beams are allowed. For laser energies of 1, 3 and 5 MJ corresponding to pellet gains of 100, 300 and 500, respectively, the trend is towards high joule outputs per Power Amplifier Module, approaching 200 kJ/PAM.

The alternative to small numbers of independent beams with high PAM energy is smaller PAMs and an optical system which maintains phase coherence among the beams so that maximum number of independent beams is still kept low. There are numerous aspects to maintaining phase coherence among several beams. A wavefront sensor and adaptive optic system is required. Motions of reflective elements in the different beam trains will cause loss of phase coherence and these path length differences must be corrected. Also, spectral uniformity and equal pressure in the PAMs must be maintained to insure that phase differences do not arise from anomalous dispersion. Such phase control systems may be feasible but clearly the entire system simplifies if PAMs can be scaled and operated reliably with output energies in the 100 kJ range.

C. POINTING

The pointing and tracking requirements for the pellet will eventually depend upon the final choice for pellet size, target chamber radius and pellet trajectory accuracy but a preliminary set of guideline⁽³⁾ can be summarized as follows:

- a. 80% of the energy in a beam line must fall within a 1 mm spot
- b. minimum distance from last optical element to the target is 10 m
- c. each beam line must be pointable to a spot on the pellet within a radius of 150 μ m

- d. the actual pellet position may vary from a nominal position by 1-2 cm
- e. pellet velocity is 10-100 m/s
- f. acquisition time is 10-100 ms for tracking the pellet and adjusting the optics appropriately

The last three requirements indicate that the (full angle) pointing accuracy is $300 \mu\text{m}/10 \text{ m} = 30 \mu\text{rad}$ as compared with a diffraction limited beam spread of $\sim 20 \mu\text{rad}$ for a 1 m mirror. Beam pointing systems with $30 \mu\text{rad}$ accuracy are within state-of-the-art; however, the angular corrections of $(1-2 \text{ cm})/10 \text{ m}$ or $1/2-1 \text{ mrad}$ may be difficult to achieve in the 10-100 ms allotted for this correction.

We estimate the pointing requirements for the "spill" of energy from the pellet. Two factors contribute to this spill: (1) the size of the focal spot relative to the pellet, and (2) the ability to center the spot on the pellet. For ease of calculation, a Gaussian approximation to the focal spot energy distribution is assumed,

$$I_p = I_o e^{-\rho^2/2\sigma^2}$$

where ρ is the radial dimension centered on the pellet and σ is the width of the distribution. The fraction of total energy within a circle of radius ρ centered on the pellet is given by

$$F = 1 - e^{-\rho^2/2\sigma^2}$$

and for $F = 80\%$, the half width of the distribution must be

$$\sigma = 0.56 \rho$$

For a pellet diameter of 1 mm, the half-width of the distribution is $\sigma = 0.28 \text{ mm}$. Carrying the Gaussian beam assumption, the focal spot radius is given by

$$\sigma = \frac{\sqrt{2}}{\pi} \frac{\lambda R}{D} \quad (\text{XDL})$$

where XDL refers to the beam quality in terms of number of times diffraction limited. For a 100 kJ beam and a mirror fluence of 10 J/cm^2 , a mirror diameter, D , of 1.15 m is required, giving a focal spot size for a chamber radius of 10 m of $\sigma = 0.04 \text{ (XDL) mm}$. Thus, a beam quality of ~ 6.7 times diffraction limited could be tolerated within the 80% enclosed energy specification. (At this point a simple comparison can be made without resorting to the Gaussian approximation. The beam profile from a well saturated power amplifier is flat, giving rise to an Airy disc pattern at the focal plane. The central portion contains 88% of the full energy and corresponds to a spot radius of $\sigma = 1.22 R / D \text{ (XDL)}$. Comparing σ with the pellet radius allows a beam quality of $\text{XDL} = 4.4$. Applying the same 88% criteria to the Gaussian approximation, the beam quality must be better than $\text{XDL} = 5.9$ or about a 14% error as compared to the flat beam profile.)

The fact that the beam may not be centered on the pellet and its effect on the enclosed energy specification may be considered by approximating the pellet by a Gaussian shape function as indicated in Figure 18. For small displacements, this is an accurate approximation to the pellet which is a "flat top" distribution. One then has the product of two Gaussian functions with one displaced from the other. If the width of the laser focal spot and pellet distribution are characterized by σ and σ_p , respectively, and the displacement of the two Gaussians is given by δ , then a simple form for the enclosed energy can be obtained by shifting to a coordinate system shifted by an amount $\beta = \delta (\sigma_T / \sigma)^2$ where $\sigma_T^2 = \sigma^{-2} + \sigma_p^2$ since for that coordinate shift the product of the two Gaussians is itself a symmetric Gaussian. For small displacements, the fraction of energy contained within a radius r_p is given by

$$F = F_0 \exp (-\delta^2 F_0 / r_p^2)$$

where F_0 is the fraction of energy within r_p with zero displacement. In Figure 19 a plot of the allowable beam quality in terms of "times diffraction limited" vs the beam pointing accuracy (δ/ρ) required to maintain 80% of the energy within the pellet radius is shown. For displacements of a few tenths of the pellet radius, the allowable degradation in beam quality is still large for the 1.15 m mirror and 10 m distance to the pellet. At δ/ρ of 0.2 and $P = 1/2 \text{ mm}$, the pointing accuracy is $10 \mu\text{rad}$ which is within near term projections of large mirror pointing technology.

The sources of poor beam quality arise from distortion in the beam from the master oscillator, refractive index gradients in the PAM and arowindow, (or salt window if used) and the

$$\text{BEAMLINE ENERGY} = 2 \int_0^{\infty} I_0 \exp(-r^2/2\sigma^2) 2\pi r dr$$

$$\text{GAUSSIAN APPROXIMATION TO PELLET: } \pi r_p^2 = 2 \int_0^{\infty} A \exp(-r^2/2\sigma_p^2) 2\pi r dr$$

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Figure 18 Gaussian Approximation for Focal Spot Displacement

Figure 19 Pointing Accuracy Tradeoff with Beam Quality

phase distortions in the optical surfaces. The first source can be eliminated by spatial filtering, albeit with a loss of signal and, hence, more preamplification to make up the difference. The phasefront errors in the PAMs will be discussed in following sections. Errors in the mirror figure are discussed here.

The final focusing mirror will have the highest fluence loading and at 10 Hz corresponds to an absorbed power of ~ 1 W/cm² assuming an absorption coefficient of 1%. At this low power level it is probable that figure errors due to thermal distortions will be negligible and only initial figures need be considered. Figure measurements on large OFHC copper mirrors which were diamond turned by the DOE Y-12 facility at ORNL⁽⁴⁾ indicate that it will soon be possible to diamond turn mirrors up to 2 m in diameter with figures better than $\lambda/2$ (peak-to-valley) in the visible. In the IR, this figure converts to a phase error of

$$\phi_{\text{rms}} = \frac{1}{\sqrt{2}} \left(\frac{0.5\mu}{10.6\mu} \right) (1/2 \text{ wave}) \frac{2\pi \text{ rad}}{\text{wave}} = 0.1048$$

A conservative estimate for the number of mirrors in a beamline is 25 giving a total rms figure error of

$$\sum (\phi_{\text{rms}})^2 = 25 (0.1048)^2 = 0.27$$

and a Strehl ratio of

$$I/I_0 = e^{-\sum (\phi_{\text{rms}})^2} = 0.76$$

which corresponds to an XDL of 1.15. From these calculations it would appear that mirror quality will not be a major contributor to the phase error budget of the system.

D. TRACKING

The problem of tracking the pellet in its flight towards the focal region of the laser requires a sensing system in both position and velocity. Once sensing is accomplished, there are two types of correcting systems, closed loop and open loop. Closed-loop pointing requires that a measurement is made of the beam spot on the pellet and that the pointing corrections are

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made within the pulse duration to bring the error to zero. Obviously, within the nanosecond pulse duration, this fully closed-loop system is impossible.

If the laser system has good pulse to pulse reproducibility, i.e., less than $10 \mu\text{rad}$ of jitter, then a modified closed-loop pointing system may be used. In this concept a measurement must be made of the position of the focused beam on the pellet. This position data is referred to a fixed reference frame and it is assumed that the next pulse will hit the same point in the reference frame. The modified closed-loop concept does still require that the position of the focal spot on the pellet be determined by observing some characteristic return from the pellet. This measurement is complicated by the fact that (1) any return pulse will be accompanied by enormous radiation from the fusion reaction, and (2) if the beam misses the pellet altogether, there is no simple method of determining where the beam went. In the latter case some type of search or random success would be required to first acquire the return or to reacquire it.

For these reasons, an open-loop pointing should be considered for the function of putting the beam on the target. However, due to the very large variations in pellet trajectory, a closed-loop system will still be required to track the pellet. One concept for detecting pellet position and velocity is shown in Figure 20 where the pellet is passed through two or more thin planes of (visible) laser illumination and scattering from the pellet is detected by triangulation detectors. Wavelength filtering and temporal nature of the signal as the pellet passes through the laser beam should provide good background rejection.

Preliminary calculations indicate the feasibility of the detection concept as follows: for a pellet with diameter, r_p , passing through a sheet beam of intensity p , the diffusely scattered light picked up by a detector located at a distance R from the pellet is

$$\phi_R = \phi_P \frac{\pi r_p^2 p}{4\pi R^2}$$

where the diffuse reflection coefficient is estimated at 50%. For a 1 mm pellet at a distance 10 m from the detector, the scattered return is $\sim 3 \times 10^{-10} \phi_P \text{ W/cm}^2$. Values of

Figure 20 Pellet Tracking System Concept

the Noise Equivalent Power of $\sim 10^{-10}$ W/cm² are easily obtained in the near IR or visible giving a signal/noise ratio of 10 with ~ 3 W/cm². A combination of narrowband laser and line filter in front of the detector should eliminate background radiation.

An estimate of the limit of measurement precision is given by

$$\sigma_x \approx \frac{80\% \Delta x}{S/N}$$

where σ_x is the standard deviation of the position measurement

Δx = resolution element size

S/N = signal to noise ratio

For fixed detector arrays, the ratio of $\sigma_x/\Delta x \sim 1/2$ and if a standard deviation of 10% of the pellet diameter is required, the resolution element $\Delta x \approx 0.2$ mm. At a viewing distance of 10 m the field-of-view is 0.2 mm/10 m or 20 μ rad. For a diffraction limited system in the visible the detector aperture is ~ 2 cm. With sufficient signal-to-noise, i.e., S/N ~ 10 , the spatial resolution is adequate with an illumination beam width of the order of the pellet diameter. For a 1 mm pellet at a distance of 10 m from the cylindrical lens of the illumination beam, the width of the lens is only ~ 1 cm and thus easily achievable. It is evident that locating and tracking the pellet to a precision of $\sim 1/10$ of the pellet diameter is well within present technology.

III. KINETICS

A. INTRODUCTION

An appropriate kinetic description of CO₂ laser medium is a key element for the Laser Fusion Driver Study since it predicts the basic or intrinsic properties of the system, namely, intrinsic laser efficiency, small-signal gain and saturation fluence. The set of parameters which can be varied to seek optimum system operating points are numerous and the task of displaying the intrinsic laser performance quantities in a succinct and meaningful way is crucial for a system designer. In this section, the CO₂ laser system is reviewed to insure uniform terminology and to highlight the physics which is relevant to short-pulse operation. The kinetic description of the pumping process is presented and working plots are developed. A separate analytic energy extraction model is developed which is independent of the pumping process and which is shown to be in good agreement with more complete computer predictions. This separation of pumping and extraction models allows the system designer to vary operating conditions in a manner in which the effects of the total system are easily determined. The output of the kinetics description is a series of working plots which depict the optimum values of the pumping parameters such as electric field, electron density, pumping duration, cavity pressure and laser gas mixture. Using these plots, a tabulated set of input conditions is generated and, with the aid of the extraction model, the intrinsic laser performance is predicted. This set of intrinsic laser performance characteristics serves as input to the systems study.

B. KINETICS CODE BACKGROUND

A detailed kinetics code has been developed at AERL⁽⁵⁾ as well as by numerous other workers.⁽⁶⁾ The basic physics is reviewed here to point out processes and time scales which are relevant to the generation and amplification of nanosecond pulses. A simplified energy level diagram of the N₂-CO₂ system is shown in Figure 21. Electrons created by the high energy e-beam are heated by the sustainer discharge and, in turn, populate the low lying vibrational levels of the CO₂ and N₂ gases. Since the electron pumping rate into the v=1 state of N₂ is much higher than that for CO₂, the energy flow is primarily into the N₂ manifold. Energy is transferred from the v=1 state of N₂ to the 00⁰1 state of the ₃ (asymmetric stretch) mode of CO₂ with high efficiency due

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Figure 21 Simplified Energy Level Diagram of the N_2 - CO_2 System

to the close coincidence of the energy levels. The time constant for this vibrational energy transfer is an important parameter for multiple pass operation of a PAM since this parameter represents the minimum relay distance for repumping the upper (CO₂) laser level. In Figure 22 the intermolecular vibrational transfer rate coefficient is shown for various molecular collision partners. For N₂-CO₂ at 300-400°K the rate coefficient is $K(N_2, CO_2) = 5 \times 10^{-13} \text{ cm}^3 \text{ part}^{-1} \text{ s}^{-1}$ so that at atmospheric density, the time constant is

$$\begin{aligned} \tau_{vv} &= [K(N_2, CO_2)N]^{-1} \\ &= 80 \text{ ns} \end{aligned}$$

and is inversely proportional to pressure. Assuming a repumping time of 2-3 times τ_{vv} , the repump time is 200 ns at 1 atm. It follows that no vibrational repump from the N₂ energy reservoir can occur on short-pulse time scales and, hence, multipass operation is required for high efficiency.

The next time scale of interest deals with the depopulation of the upper laser level. This rate coefficient is labeled k_{d1} and is plotted on Figure 23 for various collision partners. For N₂/CO₂ laser mixtures, CO₂ is the fastest deactivator and is almost 5 times faster than N₂. For a 1:1 mixture of N₂:CO₂, the collisional deactivation time constant is

$$\tau_{d1} = k_{d1} N \psi_{CO_2}^{-1}$$

where N is the total gas density and ψ_j is mole fraction of the jth component. For the 1:1 mixture at 1 atm and ~300°K, the time constant is

$$\tau_{d1} = 8 \mu\text{s}$$

It follows that since the spontaneous emission time for CO₂ is so long, $\tau_{sp} = 5 \text{ s}$, collisional decay is the only loss mechanism and, hence, a storage time of several microseconds is possible. Since the difficulty of electrical energy pumping systems is considerable for short pulse durations, it is important to exploit the long storage time capability of CO₂/N₂ systems to realize the lowest system cost and highest pumping efficiency.

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Figure 23 Upper Laser Level Depopulation Rate Constants

Once lasing has occurred, the 02^00 level of the ν_2 (bending) mode will be populated, either by intermode crossing between the two Fermi degenerate levels if 10.6μ radiation is used, directly if 9.6μ radiation is used or in combination if both bands are employed. The time constant for the $10^00 \rightarrow 02^00$ equilibration is not well known but at least one measurement⁽⁷⁾ estimates the rate at ~ 5 ns (at 1 atm) so that there is not significant equilibration between these two levels on a short-pulse time. On a vibrational repump time, however, these two levels are in equilibrium.

In gas mixtures where the CO_2 concentration is at least greater than half, the thermalization of the entire ν_2 mode occurs rapidly, and is assumed to be complete on a τ_{VV} time scale. It follows that the rate of transferring vibrational energy to translational energy, i.e., heat, is governed by τ_{d2} . The lower level rate coefficient is plotted in Figure 24 and for CO_2 or N_2 is quite low, of the order of $2-4 \times 10^{-15} \text{ cm}^3 \text{ part}^{-1} \text{ s}^{-1}$ or a deactivation time constant of $\sim 20 \mu\text{s}$. Hence, in N_2/CO_2 mixtures, there is no significant depopulation of the ν_2 vibrational manifold on a short-pulse time scale or a vibrational transfer time scale or even on a discharge pumping time scale. It is possible to increase the ν_2 deactivation rate as much as one to two orders of magnitude by adding quantities of H_2 or He as indicated in Figure 24 but as shown below, this requires impractically large quantities of either gas.

The quantity of H_2 or He which would be required for an effective lower state deactivator is estimated by requiring that the lower state deactivation rate be comparable to the vibrational transfer rate, i.e.,

$$\tau_{d2} = \tau_{VV}$$

or

$$\begin{aligned} \psi_{\text{He}, \text{N}_2} &= (\tau_{d2} N_T K_{d2})^{-1} \\ &= (\tau_{VV} N_T K_{dS})^{-1} \\ &= \frac{K(\text{N}_2, \text{CO}_2)}{K_{d1} N_T} (\psi_{\text{N}_2} + \psi_{\text{CO}_2}) \end{aligned}$$

Since the total mole fractions of all the constituent species must equal unity,

$$\psi_{\text{He}, \text{N}_2} + \psi_{\text{N}_2} + \psi_{\text{CO}_2} = 1$$

Figure 24 Lower Laser Level Depopulation Rate Constants

the helium or hydrogen fraction is given by

$$\psi_{\text{He,H}_2} = [1 + K_{d2}/K(\text{N}_2, \text{CO}_2)]^{-1}$$

Since both the rate coefficients are function of gas temperature (Figures 22 and 24), the required helium and hydrogen mole fractions are plotted in Figure 25. At room temperature, ~ 90% and ~ 35% of helium and hydrogen are needed to balance lower state deactivation and vibrational transfer times.

It is clear that such large quantities of lower state deactivants are unreasonable for several reasons: cost in the case of helium, flammability in the case of hydrogen, etc. From the kinetics standpoint, such large quantities of helium or hydrogen might result in significant upper state deactivation. The upper state deactivation rate coefficient for helium and hydrogen at 300°K is $\sim 3 \times 10^{-15} \text{ cm}^3 \text{ part}^{-1} \text{ s}^{-1}$ and $\sim 10^{-13} \text{ cm}^3 \text{ part}^{-1} \text{ s}^{-1}$, respectively (see Figure 23). These rates give a deactivation time constant of 15 μs and 1.3 μs for helium and hydrogen, respectively. For pumping pulses of several microseconds in duration, the upper state loss would be tolerable for helium but intolerable for hydrogen. These comments indicate that problems may arise in trying to achieve lower state deactivation on time scales appropriate to multipulse extraction but a more definitive answer will arise from a performance comparison on the basis of kinetic modeling.

In summary, there are three time scales of interest for a Laser Fusion Driver Laser characterized by the kinetic processes which can or cannot occur in that time. Table I lists the time scales and their important properties.

The AERL kinetics code follows in detail the three-vibrational modes of CO_2 , the nitrogen vibrational spectrum, and the energy spectrum of the discharge electrons based upon a separate Boltzmann description. The population densities and gain are calculated as a function of time by solving a separated (but coupled) differential equation for each mode in the system. Code integration can be stopped at any given point to allow for energy extraction as a one-pass amplifier, the population densities recalculated after energy extraction and renormalized as initial conditions and code integration is continued. In this manner, multipass energy extraction can be simulated in the pumping code for arbitrary multipass conditions.

The complexity of using the kinetics code in this multipass extraction mode for a wide variety of pumping conditions unnecessarily complicates a system's approach and obscures a

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Figure 25 Helium and Hydrogen Fractions for Effective Lower State Deactivation

TABLE I
TIME SCALES FOR CO₂ LASER KINETICS

1. Laser energy extraction in
~ 5 ns	1. Upper laser level in
equilibrium with v=1
mode of N ₂	1. Electron pumping mainly
through the first vibra-
tional level of N ₂
2. No repumping of upper laser
level by either electrons
or v-v transfer from N ₂ .	2. No significant repump of
upper laser level by
electrons.	2. Pump time shorter than arc
instability growth and,
hence high E/P optimum.
3. Some equilibration of the
lower laser levels of 9 and
10 μ bands may be possible
but model neglects this.	3. Both lower laser levels
(9 and 10 μband) in
equilibrium with each
other and with the
entire v ₂ vibrational
band.	3. No significant use in gas
temperature during pump
pulse.
4. No equilibration of
lower laser levels with
the remainder of their
vibrational manifolds.	4. No deactivation of upper
laser level.	
	5. No significant deactivation
of the lower laser level to
the ground state.	

physical understanding of the systems implications for various extraction schemes. The kinetics code is used primarily to calculate the pumping dynamics; namely, the input energy density, small signal gain, time to optimum pumping and upper laser level population density for various gas mixtures, cavity pressures and discharge parameters. A separate analytical model is used to calculate extraction efficiency and output energy density as a function of quantities such as number of (multi) passing and gas composition. In a select number of cases, the kinetics code was run in the pumping-multipass extraction mode to verify the validity of the model.

C. PUMPING KINETICS

An example of the kind of pumping information which is available from the kinetics code and the way in which it can be interpreted to aid in system design is shown in Figure 26. The upper state pumping efficiency is plotted vs N_2/CO_2 mixture ratio for a typical value of electron density ($2 \times 10^{12} \text{ cm}^{-3}$). The quantity E_u represents the sum of the populations of the upper laser level of CO_2 and the $v=1$ nitrogen level. This quantity represents the total energy available for lasing in a multipass configuration. The assumption is made here that once the (combined) upper state is populated and multipass energy extraction begins, no repopulation of that upper level occurs. When the ratio of E_u to E_{IN} , where E_{IN} is the input energy density, is multiplied by the 44% quantum efficiency of the CO_2 laser system, that quantity becomes the highest possible intrinsic energy efficiency for that mixture. At each value of the nitrogen fraction, ψ_N , the computer code runs have been scanned to choose the optimum value of discharge electric field, E/N . The optimum value of E/N is rather insensitive to ψ_N and ranges from $\sim 4 \times 10^{-16} < E/N \lesssim 5 \times 10^{-16} \text{ V-cm}^2$.

The effect of the increased pumping efficiency for nitrogen rich mixtures is evident in Figure 26 and for $\psi_N = 0.5$, $\sim 80\%$ of the input energy resides in the combined upper state. When this figure is multiplied by the 44% quantum efficiency, a maximum possible extraction efficiency of $\sim 36\%$ is indicated. There are at least two other physical effects which impact the final choice of the nitrogen fraction. The first has to do with the amount of lower laser level population and the fraction of CO_2 in the combined upper state. The second effect involves the number of pulses required to extract the energy. In Figure 27 the first effect is displayed as the maximum amount of energy as the population inversion in CO_2 . At low values of ψ_N , the laser gas is almost totally CO_2 and N_2 plays a minor role in the energy transfer. The difference in the upper and lower level pumping rates by electron impact is

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Figure 26 Upper State Pumping Efficiency vs. N_2 Fraction

Figure 27 Population Inversion vs N_2 Fraction

Figure 28 Number of Pulses for Efficient Energy Extraction

Figure 29 Relative Performance of Multipass Laser Extraction

Figure 30a Pumping Kinetics vs Nitrogen Fraction
 $n_e/P = 1 \times 10^{12} \text{ cm}^{-3} \text{ atm}^{-1}$

Figure 30b Pumping Kinetics vs Nitrogen Fraction
 $n_e/P = 2 \times 10^{12} \text{ cm}^{-3} \text{ atm}^{-1}$

Figure 30c Pumping Kinetics vs Nitrogen Fraction
 $n_e/P = 5 \times 10^{12} \text{ cm}^{-3} \text{ atm}^{-1}$

Figure 30d Pumping Kinetics vs Nitrogen Fraction
 $n_e/P = 10 \times 10^{12} \text{ cm}^{-3} \text{ atm}^{-1}$

not significant and, hence, the lower level is well populated, thereby reducing the population inversion. Therefore, although the upper state pumping efficiency for pure CO₂ mixtures can be as high as ~ 15%, only ~ 1/2 that amount is available for lasing due to lower level population by electron impact collisions. At the other extreme, $\psi_N \sim 1/2$, although the efficiency for pumping the combined upper state is high (~ 35% as a result of efficient N₂ pumping by electron impact collisions followed by near resonant V-V energy transfer to CO₂) the fraction of upper state population actually residing in the CO₂ state is $(1-\psi_N)$ or ~ 1/2. This effect coupled with the lower level population causes the efficiency to drop again to the ~ 8% level.

The second effect which influences the choice of ψ_N , is the number of pulses required to extract the full amount available for lasing and is shown in Figure 28. Here, f_j is fraction of energy available for lasing which is extracted by j number of pulses. This curve was generated assuming 2 band extraction so that for $\psi_N = 0$, 2/3 of $\Delta N h\nu$ comes out on the first pulse followed by 2/3 of 1/3 $\Delta N h\nu$ for the second pulse, and so on. At higher values of ψ_N , more pulses are required, e.g., ~ 4 pulses are needed to fully extract at $\psi_N = 0.25$. At the full extreme, $\psi_N \rightarrow 1$, although the combined upper level pumping efficiency is very high, very little comes out in the first pulse and it takes many pulses to fully extract the stored energy.

A cross plot of this data is shown in Figure 29 which compares the relative performance of multipass laser extraction schemes for different passes as a function of ψ_N . For low pulse numbers, the efficiency peaks at ~ 4 pulses for mixtures of $\psi_N = 0.2-0.5$. For high pulse numbers, the efficiency may peak toward higher ψ_N but it takes a significantly larger number of pulses to extraction only a slightly larger amount of energy.

These multipass conclusions are based upon an assumed redistribution of the energy in the lower laser level during the interpulse period. In this case, no bottleneaking of the lower laser level was assumed. In the following section, extraction models are explored for a wide range of assumptions.

The previous discussion indicates that ψ_N in the range 0.2-0.5 is needed for high efficiency multipass operation. In Figures 30(a) through 30(d), the quantities 44% $(E_N + E_{001})/Q$ and 44% E_{001}/Q which represent the energy stored in the combined upperstate and the CO₂ upper state alone are plotted against the nitrogen fraction, ψ_N , for n_e/P ranging from 1×10^{12} - 10×10^{12} cm⁻³ atm⁻¹. The curves plotted in

these figures represent kinetics code outputs taken just beyond the optimum pump time. In all cases, the combined upper state pumping efficiency is monotonically increasing with ψ_N while the opposite is true for the CO₂ upper state pumping efficiency. The quantity plotted on the right hand axis, $g_0 E_s$, is a measure of the CO₂ inversion density and this quantity peaks at some intermediate value. Since this quantity $g_0 E_{SAT}$ is proportional to the output energy of the first pulse (for saturated operation), it is clear that at least as far as the first pulse efficiency is concerned, a value of ψ_N intermediate of 0.2 and 0.5 is optimum. As will be demonstrated in the following sections, the absolute optimum for intrinsic efficiency will depend upon the total multipulse output and not just the first pulse output. Since it will be shown that the subsequent pulse performance depends upon ψ_N , the final choice for ψ_N is not apparent at this point.

With this background as to the general pumping efficiencies and their dependence upon quantities such as electron density, mixture ratio and pressure, the kinetics code design maps can be explained. Since there are a significant number of parameters and variables to consider, it is imperative that a simple but comprehensive display be generated to give the systems designer an intuitive grasp of the system implications for parameter choices. As an example of such a display map, Figure 31 shows a kinetics code output of a (1/4:1) laser gas mixture at 1/2 atm and a discharge electric field of $E/N = 4 \times 10^{-16}$ V-cm². This value of discharge electric field already represents an optimization for intrinsic efficiency. The quantities plotted are the ratio of energy stored in inversion density, i.e.,

$$g_0 E_{SAT} = (\sigma \Delta N) (h\nu/\sigma) = \Delta N h\nu$$

to input energy density and the input energy density. Four values of n_e/P ranging from 1×10^{12} - 10×10^{12} cm⁻³ atm⁻¹ are shown. This plot represents pumping kinetics only and no laser energy is extracted. These quantities have direct significant to the system designer because the input loading at peak efficiency sizes not only the input energy source but the total PAM volume as well. It is evident that the location of the efficiency optimum in either efficiency or input energy loading is not sensitive to electron density and at first glance it might be concluded the correct choice of electron density is of little consequence. The critical issue is that the time to reach the optimum pumping is not directly given on these plots and is a running parameter along the curves. Rather than complicate the plots with various time points, the length of the abscissa necessary to reach 1 μ s is indicated for each electron density in the upper left-hand corner. Careful

Figure 31 Kinetics Code Design Map

inspection of these 1 μ s lengths indicate that the time required to reach optimum pumping is $\sim 3 \mu$ s and $\sim 0.8 \mu$ s for $n_e/P = 1 \times 10^{12}$ and 10×10^{12} , respectively. This trend is followed for all the cases considered and forms a general theorem for kinetic performance: the higher the value of n_e/P , the larger is the input energy loading and the faster is the pump time required to load that input energy. The interesting observation, however, is that the value of the efficiency is roughly constant. All other factors equal, higher values of n_e/P would be preferable since the efficiency, input energy loading and volumetric efficiency are all high. The short electrical pulse poses a problem for the electrical design and will turn out to be a crucial issue to the limits of feasibility for pulsed power designs.

Another fundamental quantity of system design is the small signal gain, g_0 . The quantity

$$\frac{g_0 E_{SAT}}{Q} = \text{Efficiency of upper state pumping}$$

can be recast to give an expression for g_0 as

$$g_0 = \frac{(Q) (\text{Efficiency of upper state pumping})}{E_{SAT}}$$

Thus the family of curves for fixed g_0 is a series of hyperbolae when plotted on the same axes as Figure 31 if E_{SAT} is constant. Since

$$E_{SAT} = h\nu/\sigma$$

where σ is the stimulated emission cross section, the constancy of σ is monitored in the kinetics code output. For a wide range of input energy loadings and $N_2:CO_2$ mixes, $E_s = 1/3$ J/cm². At the high values of input energy density, i.e., $Q \geq 150$ J/l, E_s drops slightly due to gas heating. For the purpose of graphic display, E_{SAT} has been assumed constant. The plot of g_0 values is shown in Figure 32 and is to be used as an overlay to determine the value of g_0 for a fixed input loading and efficiency. In this way the design plot gives the input loading, upper-state pumping efficiency, time to optimum pumping, small signal gain and electron density. The remainder of the plots, Figures 33(a) through 33(h) show this information for gas mixtures 1/4:1, 1/2:1, 1:1 and cavity pressures 1/2, 1 and 2 atm. In each plot, the optimum value of E/N has been chosen by examining kinetics code outputs. A cursory examination of these plots shows that there is little variation in

Figure 32 Gain Coefficient Curves

Figure 33a Kinetics Code Design Maps
 N₂:CO₂ = 1/2:1, 1/2 atm

Figure 33b Kinetics Code Design Maps
 $N_2:CO_2 = 1:1, 1/2 \text{ atm}$

Figure 33c Kinetics Code Design Maps
 $N_2:CO_2 = 1/4:1, 1 \text{ atm}$

Figure 33d Kinetics Code Design Maps
 $N_2:CO_2 = 1/2:1, 1 \text{ atm}$

Figure 33e Kinetics Code Design Maps
 $N_2:CO_2 = 1:1, 1 \text{ atm}$

Figure 33f Kinetics Code Design Maps
 $N_2:CO_2 = 1/4:1, 2 \text{ atm}$

Figure 33g Kinetics Code Design Maps
 $\text{N}_2 : \text{CO}_2 = 1/2 : 1, 2 \text{ atm}$

Figure 33h Kinetics Code Design Maps
 $N_2:CO_2 = 1:1, 2 \text{ atm}$

operating points exactly at the optimum and, furthermore, that these optima all occur at rather low values of input energy loading. Operation at the optimum will result in large values of laser volume to reach the desired output of 1-5 MJ. To increase the input energy loading, the operating point is pushed beyond the optimum, but not allowed to drop significantly. Operating at 90% of the optimum efficiency appears to be a reasonable compromise. Tables II, III and IV show the values of kinetic properties for this 90% operating part as a function of gas mixture and cavity pressure.

Design maps can be used to address the question of helium bearing mixtures. It was shown that unless helium is present in very large quantities, i.e., $\sim 90\%$, no deactivation can occur on an interpulse time scale, i.e., hundreds of nanoseconds. At such high concentrations, upper state deactivation is likely to occur. On a somewhat longer time scale, say several microseconds, helium could be used in reasonable quantities to achieve lower state deactivation but this effect would only help in multipass configurations with many pulses. Here, diffraction, optical phase front distortion and optical system complexity will limit the number of passes and helium will be of little benefit.

The advantage to helium is that both the mass density and Gladstone-Dale coefficient are low thereby reducing the power consumption of the flow loop and reducing distortions in the optical quality as a result of beam propagation. However, since only the CO_2 and N_2 are directly involved in lasing a low density laser mixture with the same $\text{N}_2:\text{CO}_2$ ratio as in the helium buffered mix will have even lower gas density and lower Gladstone-Dale constant, and if the performance is unchanged, then helium addition is unnecessary. A comparison of the pumping performance of these mixtures is shown in Figure 34. The pumping efficiency of the combined upper state ($\text{N}_2 + \text{CO}_2$) vs the input energy loadings is plotted for three gas mixtures. In comparing the (3:1:1) mix at 1 atm and the (0:1:1) mix at 1/2 atm, the number density of active molecules is roughly the same, i.e., the density of $\text{N}_2 + \text{CO}_2$ is 0.4 atm in the (3:1:1) mix and 0.5 atm in the (0:1:1) mix. The (0:1:1) mix at 1 atm is also shown for comparison. For all three cases the E/P value is chosen to give approximately the same electron temperature, ~ 1 eV since the electron temperature is a sensitive measure of pumping efficiency. For comparison, the pumping efficiency is kept roughly constant and the pumping parameters are summarized in Table V.

Thus even though the values of E/P are much different, the E/N_M where N_M is the number density of molecular species (N_2 and CO_2) is approximately the same. Since for the two

TABLE II

KINETICS CODE OUTPUT - 1/2 ATM

Mix, N ₂ /CO ₂	N _e x 10 ¹² cm ⁻³	Q _{in} -J/L	% EFF	g _O -m ⁻¹	E-kV/cm	J-A/cm ²	T _p -μs
1/4:1	0.5	25	15	1.9	4.9	0.9	5.9
	1.0	37	16	3.0		1.9	3.9
	3.0	62	17	5.2		6.0	2.1
	5.0	72	16	5.8		8.6	1.7
1/2:1	0.5	35	18	2.6	6.2	1.2	4.8
	1.0	45	18	3.5		2.4	3.1
	3.0	70	17	5.2		5.6	2.1
	5.0	80	17	5.9		8.7	1.5
1:1	0.5	33	18	2.1	6.2	0.8	6.8
	1.0	42	19	2.7		1.7	4.1
	3.0	70	18	4.4		5.0	2.3
	5.0	85	17	5.0		6.0	2.3

TABLE III
KINETICS CODE OUTPUT - 1 ATM

Mix, N ₂ /CO ₂	Ne x 10 ¹² cm ⁻³	Q _{in} -J/L	% EFF	g _O -m ⁻¹	E-kV/cm	J-A/cm ²	T _p -μs
1/4:1	1.0	45	16	1.8	9.8	1.8	2.5
	2.0	67	16	2.8		3.4	2.0
	6.0	115	16	4.2		10.7	1.1
	10.0	142	16	5.8		17.1	0.9
1/2:1	1.0	60	18	2.3	9.3	2.1	2.9
	2.0	90	18	3.5		4.4	2.1
	6.0	140	18	5.3		13.0	1.1
	10.0	165	17	6.0		1.9	0.8
1:1	1.0	65	18	2.0	9.8	2.1	3.2
	2.0	82	19	2.7		3.8	2.2
	6.0	132	18	4.1		11.2	1.2
	10.0	170	17	5.0		19.3	0.9

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TABLE IV

KINETICS CODE OUTPUT - 2 ATM

Mix, N ₂ /CO ₂	Ne x 10 ¹² cm ⁻³	Q _{in} -J/L	% EFF	g _O -m ⁻¹	E-kV/cm	J-A/cm	T _p -μs
1/4:1	2	87	16	1.7	19.6	3.4	1.3
	5	142	17	3.0		8.2	0.9
	10	200	17	4.2		17.0	0.6
	20	200	16	4.1		34.0	0.3
1/2:1	2	110	18	2.1	24.6	3.4	1.3
	5	175	18	3.4		8.6	0.8
	10	200	18	3.8		17.3	0.5
	20	200	18	3.8		35.4	0.2
1:1	2	135	18	2.1	24.6	3.1	1.8*
	5	175	19	2.8		7.7	0.9
	10	200	19	3.3		15.3	0.5
	20	200	19	3.3		30.1	0.3

Figure 34 Comparisons of Pumping Performance for Helium Bearing Mixtures

TABLE V
SUMMARY OF PUMPING PARAMETERS

Mix (He:N ₂ :CO ₂)	Pressure (atm)	E/P kV-cm ⁻¹ atm ⁻¹	E/N _{M₂} V-cm ²	n _e /P cm ⁻³ atm ⁻¹
3:1:1	1	6.15	6.25 x 10 ⁻¹⁶	1 x 10 ¹²
0:1:1	1	12.5	5.0 x 10 ⁻¹⁶	1 x 10 ¹²
0:1:1	1/2	12.5	5.0 x 10 ⁻¹⁶	2 x 10 ¹²

cases to be compared, the (3:1:1) - 1 atm and the (0:1:1) - 1/2 atm, the number density of active molecular species is kept roughly the same, the absolute value of electron density, as opposed to n_e/P should be kept fixed as indicated in Table V. Maintaining constant n_e gives a constant state of energy input loading.

The helium rich curve (3:1:1) - 1 atm rises more slowly than the helium-free curves because at these high helium concentrations, the upper state is being slightly depopulated. This calculation is for pure pumping, no energy extraction has been done and, hence, no lower state deactivation physics enters into the picture.

In Figure 34, the maximum efficiency for all 3 mixes is $\sim 12\%$ so that the 90% point is roughly 10%. At 10% upper state efficiency, the two comparative mixtures (3:1:1) - 1 atm and (0:1:1) - 1/2 atm are both loaded to the same joule/liter loading in times of $\sim 9 \mu s$ and $\sim 6 \mu s$, respectively. The third mix, (0:1:1) - 1 atm gets loaded to a higher value in a shorter time (to the same 90% point) because the electric field is twice that of the other two cases.

The conclusions which can be drawn from Figure 34 are that:

- a. Where the maximum number of passes and, therefore, the total pulse duration is limited, helium cannot effectively deactivate the lower state.
- b. For the same number density of active gas molecules, i.e., the (3:1:1) - 1 atm and (0:1:1) - 1/2 atm mixtures, the performance levels are about the same. The timescale to load the electrical energy in the (3:1:1) case is slightly longer than for the (0:1:1) mix but both are already long enough for practical pulsed power systems.
- c. In comparing the situation at the same pressures, i.e., the (3:1:1) - 1 atm and the (0:1:1) - 1 atm cases, the helium free mix loads more electrical energy into the gas at the same efficiency albeit in a slightly shorter time.

There appears to be no clearly defined advantage to laser gas mixtures with high helium concentrations in terms of energy loading or efficiency and being the most expensive species in the mixture, its use is not warranted. If, on the other hand, the laser pulse duration requirement drops to ≤ 1 ns the addition of helium will increase the rotational relaxation rate and increase the short pulse extraction efficiency.

D. EXTRACTION MODEL

The kinetics code description of the pumping dynamics has omitted energy extraction to focus on the physical effects involved. In the simplest case, the laser gas mixture is pumped to be optimum point, or to a predetermined efficiency beyond the optimum, the pumping is turned off and extraction of energy is begun by multipassing short optical pulses. An alternative is to extract energy during the pumping pulse and, from a parasitics consideration, is of some benefit.

As an illustration of a simple case, consider ψ_N to be near unity so that the nitrogen reservoir is large and not significantly depleted during the first few multipass pulses. Furthermore, it is assumed that with no helium or hydrogen, there is no deactivation of the lowest lying vibrational levels. The output energy density from a single pass amplifier is given by

$$E_{OUT} = E_{SAT} \ln \left[1 + e^{g_0 \ell} (e^{E_{IN}/E_{SAT}} - 1) \right]$$

where E_{IN} , E_{OUT} and E_{SAT} are the input, output and saturation energy densities, respectively. For saturated operation, $E_{IN}/E_{SAT} \gtrsim 1$, and the output becomes

$$E_{OUT} = E_{IN} + E_{SAT} g_0 \ell$$

Before the first (optical) pulse arrives, the CO_2-N_2 system has been pumped to an inversion density corresponding to a small signal gain-length product, $g_0 \ell$. Assuming that the first pulse is at a saturating level, the normalized output is given by

$$E_{OUT}/(E_{SAT} g_0 \ell) \cong 1$$

if $g_0 \gg 1$. After this first pulse passes, the upper and lower levels are equilibrated to 1/3 (assuming extraction on both the 9 and 10 μ bands). After a few τ_{VV} , the upper CO_2 level is repumped by the nitrogen reservoir to the unity level as before so that the population inversion in unity minus the lower state population. The lower state population reaches a quasi-equilibrium among several states in times of the order $\tau_{d3} = 5-10$ ns (see Figure 21). Therefore, in the interpulse time of $\sim 2\tau_{VV}$ ns, a quasi-equilibrium is reached among several states, namely the $10^0 0$ and the entire vibrational bending mode manifold, $0nJ0$ where $n = 1, 2, 3, \dots$ and j corresponds to the levels in Figure 21. The degeneracy of level j in the bending mode is $(j + 1)$ and the relative population of each

level in this vibrational manifold is related by the Boltzmann factor $e^{-\epsilon/KT}$ where ϵ is the energy spacing between levels, assumed equal for the first few levels (harmonic oscillator). The population of the following levels are approximately equal since they are very close in energy

$$n(10^00) = n(02^00) = n(02^20)$$

but the last term must be taken 3 times due to the $j + 1$ degeneracy. Also the relation between the 02^00 and 01^00 modes are given by

$$\frac{n(01^10)}{n_0} = \exp(-\epsilon/KT)$$

$$\frac{n(02^00)}{n_0} = \exp(-2\epsilon/KT) = \frac{n(01^00)}{n_0}^2$$

The population in the next higher level is neglected as a first approximation and, thus, after the $2\tau_{VV}$ time delay, the populations in the lower states are distributed according to

$$2n(01^10) + n(10^00) + n(02^00) + 3n(02^20) = 2/3$$

or

$$2 n_L + 5 n_L^2 = 2/3$$

where $n_L = n(01^10)$. Solving this quadratic equation for the lower laser level population

$$n_L = 0.22$$

and

$$n(10^00) = n(02^00) = (0.22)^2 = 0.05$$

After vibrational repumping of the upper laser level, the gain is proportional to the population inversion

$$\begin{aligned} n &= n(00^01) = n(10^00) \\ &= (1 - 0.05) = 0.95 \end{aligned}$$

or

$$g_1 = 0.95 g_0$$

and the output energy after passing the previous pulse is

$$E_{OUT}/E_{SAT} g_0 = 1 + 0.95$$

After this second pulse extraction the upper and lower state populations are equal and given by

$$3X = 1 + 2(0.05) = 0.37$$

and the lower state is filling up, but very slowly. We repeat the process of equilibrating all the lower levels and generate the following table

Pulse Number	1	2	3	4	5	6	7	8	9	10
g/g_0	1	0.95	0.88	0.82	0.70	0.65	0.60	0.55	0.45	0.40
$E_{OUT}/(E_{SAT} g_0)$	1	1.95	2.83	3.65	4.35	5.00	5.60	6.15	6.60	7.00

This calculation shows that even if there is no depletion of the nitrogen reservoir, lower state bottlenecking limits efficient pulse extraction to about 10 passes.

To see the effect of a finite nitrogen reservoir, the calculation is repeated for a 1:1 mix of $N_2:CO_2$. The only difference between this calculation and the one above is that after passage of the first pulse and vibrational repump from the (finite) nitrogen reservoir, the upper laser level population is

$$(1 + 1/3)/2 = 0.67$$

and the process repeats. The following tabulated values result for the 1:1 mix:

Pulse Number	1	2	3	4	5	6	7	8	9	10
g/g_0	1	0.65	0.35	0.18	0.12	0.08	0.02	0.01	0.01	0.01
$E_{OUT}/E_{SAT} g_0$	1	1.65	2.00	2.18	2.30	2.38	2.40	2.42	2.43	2.44

Here, it takes about 4 passes to efficiently extract all the available energy.

For the other extreme case, where the function of CO_2 is much larger than that for N_2 it is obvious that only a few pulses are needed to extract the energy because there is no N_2 reservoir to feed multiple passes. These three cases and intermediate representative values are plotted in Figure 35.

Figure 35 Multiplex Extraction Model for No Lower State Deactivation

The curves in Figure 35 are normalized to $E_{SAT} g_0$ which is proportional to the population inversion, Δn . In absolute terms, it is the value of n convoluted with the maximum number of passes which determines the total energy which can be extracted. For example, the curve labeled [00:1] represents an infinite N_2 bath. Seven pulses can extract almost five times $E_{SAT} g_0$ but for a fixed pressure, the amount of CO_2 is small (since the ratio of CO_2/N_2 is small) and $E_{SAT} g_0$ is a small number in absolute terms.

This calculation is to be compared with that illustrated in Figure 26 which gives the efficiency of just upper state pumping. That calculation predicts that if the initial lower state population is neglected and, if any subsequent lower state population is deactivated away, the maximum efficiency is $\sim 36\% \times 2/3$ or 24%. Bottlenecking in the lower state as represented by the [00:1] mixture in Figure 35 can only reduce this estimate.

The extraction calculation is now repeated to assess the effect of some lower state deactivation. As seen above, helium is not well suited as a lower state deactivator because too much is required. Hydrogen is cheaper and because of larger deactivation rates, lower concentrations are required. It is seen from Figures 23 and 24 that the ratio of τ_{d2}/τ_{d1} , i.e., the ratio of lower to upper state deactivation times is higher for helium than for hydrogen,

$$\begin{aligned}\tau_{d2}/\tau_{d1} &= 1.5 \times 10^{-12}/1.2 \times 10^{-13} = 12 - \text{hydrogen} \\ &= 6 \times 10^{14}/2 \times 10^{-15} = 30 - \text{helium}\end{aligned}$$

Therefore, for a given amount of lower state deactivation, corresponding to a given hydrogen or helium pressure, the upper state will be deactivated = 3 times faster with hydrogen than with helium.

If sufficient hydrogen is added to make $\tau_{d2} = \tau_{vv}$ then the lower state will be deactivated by e^{-1} at the end of an interpulse equal to τ_{vv} . During that same interval, the upper state will be deactivated by $\sim e^{-0.1}$ which is probably tolerable. Note, however, that most codes require an initial pump up time of several microseconds before the first pulse, and, therefore, some initial loss of the upper state will occur even with the 12:1 lower to upper state deactivation preference.

The previous calculation is repeated assuming that the population in the $[01^10]$ state (degeneracy of 2) is reduced by 1/3 between pulses. Figure 36 shows the result of the deactivation. The roll off at pulse numbers = 5 is eliminated for high N_2 fractions.

Figure 36 Multiplex Extraction Model Assuming Partial Deactivation of the Lower Laser Level

Finally, this calculation is repeated for the extreme case of complete lower state deactivation so that lower state bottleneaking is eliminated. This situation is impossible to realize in practice because of the high concentrations required but it is instructive to sort out the physical effects. The result of this calculation is shown in Figure 37.

The three cases are summarized in Figure 38 for representative mixes of $\psi_N = 1, 1/2, 0$. For $\psi_N \leq 1/2$ the effects of lower state deactivation are relatively important since for the few number of pulses which extract the majority of the stored energy, the reservoir provided by the other states in the 2 vibrational manifold is sufficient.

For nitrogen fractions of interest, $0.2 < \psi_N < 0.5$, lower state deactivation is unimportant, and an analytical model for this case can be formulated as follows:

Assuming no lower state population, the pulse energy extracted after 1 pulse is:

$$E_1 = E_u \frac{2}{3} \psi_C E_u \frac{2}{3} (1 - \psi_N)$$

where E_u is the combined upper state energy. Assuming the lower states are completely deactivated and that the CO_2 and N_2 upper states are equilibrated before the arrival of the second pulse, the second pulse energy is

$$E_2 = \frac{2}{3} \psi_C (E_u - \frac{2}{3} \psi_C E_u) = \frac{2}{3} \psi_C E_u (1 - \frac{2}{3} \psi_C)$$

The n^{th} pulse energy is therefore

$$E_n = \frac{2}{3} \psi_C E_u (1 - \frac{2}{3} \psi_C)^{n-1}$$

and the total pulse energy after n pulses is

$$E_{Tn}/E_u = f_n(\psi_N) = \frac{2}{3} (1 - \psi_N) \sum_{j=0}^{n-1} [1 - \frac{2}{3} (1 - \psi_N)]^j$$

The quantity $f_n(N)$ was plotted in Figure 28. If the identification that

$$E_{\text{OUT}} (\text{first pulse}) = E_{\text{SAT}} g_o = \frac{2}{3} E_u [(1 - \psi_N)]$$

Figure 37 Multiplex Extraction Model with Complete Lower State Deactivation

Figure 38 Summary of Multiplex Extraction Models

is made, the plot of

$$E_N / (\frac{2}{3} \psi_C E_u) = \sum_{j=0}^{n-1} [1 - \frac{2}{3} (1 - \psi_N)]^j$$

is precisely the curves shown in Figure 37.

As a check against the kinetics code, the code was modified to include pulse extraction for one case and the result plotted in Figure 39. The analytical model is shown for 5 pulses on the right. For values of ψ_N between 0.2 and 0.5, the 5 pulse extraction model accurately predicts the total energy extraction.

In the previous analysis, it was assumed that the pumping was continued until the optimum efficiency, or slightly beyond, was obtained followed by multipulse extraction. There are reasons why extraction during the pumping pulse may be beneficial, e.g., suppression of parasitics. Although the individual pulse amplitudes may vary for the two cases, the total integrated energy output is similar whether energy extraction was delayed until the end of the pumping pulse. This concept was tested by running a kinetics code case with extraction and the comparison is shown in Figure 10. Two pulses out of the five were extracted at the tail end of the pumping pulse and the outputs in the two case differed by $\lesssim 5\%$.

The two-part model; namely, kinetics code for pumping efficiency and the analytical model for energy extraction is a reasonable approximation to a real pumping extraction scenario since the total multipulse time is not a large fraction of the total pump time, i.e., 5 pulses at 200 ns per interpulse time of $\sim 1 \mu s$ total as compared to a pumping pulse of several microseconds. Therefore, in any reasonable configuration, the extraction pulses will tend to appear at the very end of the pumping pulse rather than early on.

J1199

Figure 39 Computer Check of Multiplex Extraction Model

IV. POWER AMPLIFIER MODULE CAVITY DESIGN

A. INTRODUCTION

The kinetics code and extraction model specified the intrinsic properties of the laser mix, i.e., characteristics of the gas that applied on a unit volume basis without regard to cavity dimensions or terminal properties. This intrinsic information is used to design the remainder of the systems and in particular, the dimensions of the PAM cavity.

B. GAIN LENGTH

There are several aspects to the choice of gain length for a CO₂ Laser Fusion Driver operating in the storage mode, i.e., energy is pumped in over a relatively long period and the laser amplifier must hold or store this energy until arrival of the short optical pulse. A prime consideration for the limit on the gain length is the holdoff properties of the PAM and the amount of gain-length product which can withstand the onset of parasitics.

A second aspect to the choice of gain length is the aperture fluence limit which in itself is set by several constraints. Since these fluence limits depend upon the gain-length product in all cases, the quantity of interest is not just length in the optical direction, but the product of gain and length. Furthermore, to avoid confusion regarding the gain which can differ from the small signal or unsaturated value when flux is present, unless otherwise stated, the gain will refer to the small signal value, g_0 , and all calculations will be referenced back to the value.

There are two aspects to the parasitic limit, a steady-state and a transient or "time of flight" limit. In the steady-state limit, if the gain-length product is sufficiently high, internal feedback from diffuse reflections can cause a mode to grow into parasitic oscillation even if the reflections are quite small. The limit on gain standoff depends critically on the particular design of the cavity especially with regard to the internal structures and how they will have been placed and coated for antireflection. Early experiments at CO₂ wavelengths in Gasdynamic Lasers (GDL) achieved gain standoffs at $g_0 l \approx 12.5$ and more recent work with pulsed e-beam amplifiers pushed this limit to ~ 14 . The actual value of gain standoff for a Laser Fusion Driver would also depend upon the optical configuration of the multipass geometry since even mirror at 45° will feedback a small fraction due to surface imperfections in the mirror surfaces. As an example of the factors which enter into the gain standoff limit, consider the PAM-optical configuration shown in Figure 40(a). A PAM with gain length L_g and small

Figure 40 Geometries for Parasitic Calculations
 (a) PAM geometry
 (b) Unfolded PAM geometry

signal gain, g_o , is set within a multipass cavity indicated schematically by the two 45° mirrors separated by L_M . Parasitic oscillation will occur if the round trip gain exceeds unity. Referring to the unfolded geometry of Figure 40(b), a pulse of light is tracked from its start at the left through the system to its starting point.

The pulse of light is increased by two (small signal) gain paths, and decreased by two reflections and the total solid angle of acceptance.

$$\frac{d^2}{4\pi (2L_M)^2} (1\%)^2 e^{2g_o L_g} > 1$$

or

$$g_o L_g < \frac{1}{2} L_N \left[\frac{4\pi (2L_M)^2}{d^2} \times 10^{+4} \right]$$

For this typical calculation, $L_M = 60$ m corresponding to 200 ns vibrational transfer of one atmosphere, $d = 1.5$ m (square PAM aperture) and a diffuse reflectance of 1% is assumed

$$g_o L_g < 10.25$$

It is to be noted, however, that this parasitic limit is a steady-state limit in the sense that if the round trip gain is greater than unity, a noise signal will build up to a parasitic model after a certain number of transits. In laser systems where there can be many transits, this is the important criterion for parasitic buildup. It is not surprising that the numerical value of $g_o L_g$ for typical laser systems is roughly the same value although individual cavity designs differ. The experimental nature of the small signal dependence and the observation that diffuse reflections from structural surfaces do not generally vary by a large amount give rise to a practical upper limit of ~ 10 .

In a Laser Fusion Driver which will not have long pump times, there may not be sufficient transit times to reach a parasitic mode. On the other hand, for a multipass configuration, a single ray of light would make several passes through the medium and, hence, the total gain-length product could be very large. The following calculation indicates the factors involved in estimating the parasitic limit.

The time required for gain buildup of a noise signal is given by

$$\frac{d\phi}{dx} = g_o \phi + \text{noise}$$

The noise term in this case if fluorescence given by

$$\text{noise} = \frac{n \, h\nu}{\tau_s}$$

where

n = upper state density

$h\nu$ = CO_2 laser photon energy = 1.87×10^{-20} J/photon

Ω = solid angle

τ_s = spontaneous lifetime ~ 5 s

Integrating this equation subject to the boundary conditions

$$\phi(X = 0) = 0$$

the flux is obtained as

$$\phi(X) = \frac{n \, h\nu}{\tau_s} \frac{\Omega}{g_o} (e^{g_o X} - 1)$$

and solving for $g_o X$;

$$g_o X = \ln \left[1 + \frac{\tau_s g_o \phi(x)}{n \, h\nu \, \Omega} \right]$$

The gain-length product which allows ϕ to grow to the saturation flux,

$$\phi = \phi_s = \frac{h\nu}{\sigma \tau_c}$$

where

σ = stimulated emission cross section

τ_c = collision or upper state deactivation time

In the case of helium-free laser mixes, $\tau_c = \tau_{d_1} = (K_{d_1} N)^{-1}$
 where

K_{d_1} = upper state deactivation rate constant
 N = total number density

Referring to Figure 23 at 300°K

$$K_{d_1} \cong 10^{-14} \text{ cm}^3 \text{ part}^{-1} \text{ s}^{-1}$$

so that at atmospheric density

$$\tau_{d_1} \cong 4 \text{ } \mu\text{s}$$

The estimate of the maximum gain-length product is further simplified by noting

$$g_o = n\sigma$$

$$(g_o L)_{\max} = \ln \left(1 + \frac{\tau_s}{\tau_{d_1} \Omega} \right)$$

The solid angle is device dependent but as it turns out, the solid angle does not differ by a significant amount for many major CO₂ lasers. For example, the GDL had cavity dimensions of 4 x 4 x 100 cm for a solid angle of

$$\Omega \cong \frac{(4 \text{ cm})^2}{4\pi (100 \text{ cm})^2} \cong 1.3 \times 10^{-4} \text{ ster}$$

For a typical PAM geometry the solid angle could be

$$\Omega \cong \frac{(1 \text{ m}) (2 \text{ m})}{4\pi (60 \text{ m})^2} \cong 4 \times 10^{-5} \text{ ster}$$

Using a value of $\Omega = 10^{-4}$ ster, the maximum value of $g_o L$ is

$$(g_o L)_{\max} \cong \ln \left(1 + \frac{5 \text{ s}}{4 \mu\text{s}} \frac{1}{10^{-4} \text{ ster}} \right) \cong 23$$

For the multiplex model with 5 passes and 60 m/pass or a total path length of ~ 300 m the maximum value of $g_o L$ becomes

$$(g_o L)_{\max} \cong 27$$

The conclusion is that a one way (small signal) amplification product of only 27 is allowed before a signal grows out of the noise to a level comparable to saturation fluence. In the Laser Fusion Driver case, the maximum small signal gain-length product is $\sim 9-10$ so that for 5 passes this parasitic limit is easily exceeded. However, the model requires more sophistication because, first, the gain in the amplifier is a function of time during the pumping pulse and only reaches maximum gain at the end of the pumping pulse and, second, energy extraction pulses during the pumping pulse can lower the gain. A worse case model is depicted in Figure 41. A linear time varying gain profile

$$g = g_o t/t_p$$

where t_p is the pulsewidth of the PFN is assumed. According to Figure 40(b), the transit time through and between the gain media are L_g/C and $(L_M - L_g)/C$, respectively. The worst case is a noise pulse which starts at t_o such that the noise pulse sees the maximum gain through the system. The analysis is simplified by noting that in an exponential gain medium, the noise source, is dominated by the source term at the very beginning. Hence, the equation for the flux is:

$$10_{\text{pass}} \left\{ \begin{array}{ll} \frac{1}{c} \frac{d\phi}{dt} = 0 & 0 < t < t_o + \frac{L_M - L_g}{2c} \\ \phi = \phi_o = \frac{nh\nu_{\Omega}}{\tau_s} & t = t_o + \frac{L_M - L_g}{2c} \\ \frac{1}{c} \frac{d\phi}{dt} = g_o \frac{t\phi}{t_p} & t_o + \frac{L_M + L_g}{2c} < t < t_o + \frac{L_M + L_g}{2c} \\ \frac{1}{c} \frac{d\phi}{dt} = 0 & t_o + \frac{L_M - L_g}{2c} < t < t_o + \frac{L_M}{c} \end{array} \right.$$

Figure 41 Conservative Model for Buildup of Amplified Spontaneous Emission

At the end of one pass, the gain is given by

$$\begin{aligned}\ln \phi / \phi_o &= \frac{cg_o}{2t_p} \left[\left(t_o + \frac{L_M + L_g}{2c} \right)^2 - \left(t_o + \frac{L_M - L_g}{2c} \right)^2 \right] \\ &= g_o L_g \frac{t_o}{t_p} \left(1 + \frac{1}{2t_o} \frac{L_M}{c} \right)\end{aligned}$$

This analysis is extended to the next pass since no gain occurs in the space between the PAMs. The relevant equation to solve for the second pass is

$$\frac{1}{c} \frac{d\phi}{dt} = g_o \frac{t\phi}{t_p}; \left(t_o + \frac{-L_M}{c} \right) + \frac{L_M - L_g}{2c} < t < \left(t_o + \frac{L_M}{c} \right) + \frac{L_M + L_g}{2c}$$

and the initial value of ϕ is the final value at the end of the first pass. Because of the additive property of logarithms, the total gain is

$$\begin{aligned}\ln \phi_2 / \phi_o &= \ln (\phi_2 / \phi_1) (\phi_1 / \phi_o) = \ln \phi_2 / \phi_1 + \ln \phi_1 / \phi_o \\ &= g_o L_g \frac{(t_o + L_M/c)}{t_p} \left(1 + \frac{L_M}{2(t_o + L_M/c)c} \right) \\ &\quad + g_o L_g \frac{t_o}{t_p} \left(1 + \frac{L_M}{2t_o c} \right)\end{aligned}$$

and at the end of N passes,

$$\ln \phi_N / \phi_o = \frac{g_o L_g}{t_p} \sum_{n=0}^{N-1} (t_o + nL_M/c) \left(1 + \frac{L_M/c}{2(t_o + nL_M/c)} \right)$$

Further, the worst case where the noise pulse transit begins at a time such that the last pulse passes at the highest gain, i.e., at the end of the pumping pulse, requires that

$$t_o = t_p - NL_M/c$$

so that

$$\ln \phi_N/\phi_o = \frac{g_o L_g}{t_p} \sum_{n=0}^{N-1} \left[t_p + (n-N)L_M/c \right] \left[\frac{1 + \frac{1}{2} L_M/c}{(t_p + (n-N)L_M/c)} \right]$$

To keep the noise pulse from growing to the saturation flux $\ln \phi_N/\phi_o \lesssim 27$

$$27 \leq g_o L_g \sum_{n=0}^{N-1} [1 + (n-N)A] \left[1 + \frac{1}{2} \frac{A}{1 + (n-N)A} \right]$$

where

$$A = \frac{L_M/c}{\tau_p}$$

In this case, at one atmosphere, the parameter

$$A \cong \frac{60 \text{ m/3} \times 10^8 \text{ m/s}}{2 \text{ } \mu\text{s}} = 0.1$$

The quantity $g_o L_g$ for $N=5$ and various values of A is plotted in Figure 42. The calculation is stopped at $A = 0.2$, since for five passes the entire pump time is used and the single pass limit of $g_o L_g \cong 10$ (internal parasitics) is approached.

At 1/2 atm, there are several cases where $t_p = 4 \text{ } \mu\text{s}$ so that at 120 m mirror separation again $A = 0.1$. If the system is made more compact at high pressure the pumping pulse time must also drop to maintain the same value of A . If t_p is kept at $2 \text{ } \mu\text{s}$, A drops to 0.05 and the maximum gain decreases.

This calculation yields an overestimate of the maximum gain-length product since the extraction pulses which were taken during the pumping regime did not alter the shape of the gain profile or

Figure 42 Maximum Small Signal Gain Length Product from ASE Model

the final value of g_o attained. In reality, the shape of the pumping pulse is not linear when extraction takes place during pumping and the gain profile rolls over at late time. The final value of g_o reached is less than for the case of zero extraction. Hence, higher gain-length products can be accommodated.

In summary, the maximum gain-length product which can stand off the parasitic limit sets the length in the optical direction once the small signal gain is known from the kinetics code output. The only other variation which could impose a lower limit on the gain length is a diagonal constraint as indicated in Figure 43. For PAM designs where the length, width and height are comparable, the maximum length of the cavity, i.e., the diagonal,

$$L = \sqrt{L_c^2 + H^2 + W^2}$$

can vary significantly from the gain length, L_c . In this case, the parasitic limit based upon internal reflections will limit L to be

$$L < (g_o L)_{\max} / g_o$$

and this constraint will in turn, limit L_c since the diagonal constraint. such a lower limit on L_c may be more, a more severe constraint on L_c than that based upon time-of-flight isolation.

The second aspect of the design constraints on the gain length arise from material limits on the maximum fluence out of the PAM for N pulses. Assuming that the input to the PAM is equal to the saturation fluence, the output energy density is

$$E_{OUT} = E_{SAT,O} P (1 + \frac{2}{3} N_{eff} g_o L_c)$$

where the pressure dependence of the saturation fluence has been displayed explicitly. For pressures up to a few atmospheres, E_{SAT} varies linearly with pressure. At higher pressures, the saturation fluence "saturates" and becomes independent of pressure. For operating pressures which are relevant for Laser Fusion Drivers, the linear variation is appropriate. In Figure 44 the output fluence is plotted versus $g_o L_c$ for various pressures and values of N_{eff} which for $N = 5$ and $0.2 < \psi_N < 0.5$ range from $1.8 < N_{eff} < 2.6$.

For aerowindow operation, the mirror damage limit becomes the lower limit. Laser induced breakdown in the laser gas mix is also plotted for pure CO_2 (worst case in a helium free mix).

J2268

Figure 43 PAM Geometry

Figure 44 Output Fluence Limits

The limit for laser induced breakdown is monotonically decreasing with pressure and the 10 J/cm^2 is an estimate at 2 atm and hence is also a worst case estimate. For the aerowindow option, at all but the highest pressures and highest values of N_{eff} , the $g_0 L_c$ limit due to mirror damages is not exceeded and the parasitic limit dominates. For the salt window option, however, the absolute damage threshold of 5 J/cm^2 (for nanosecond pulses) and the working limits (which are not well known for long life operation) dominate the $g_0 L_c$ limit at pressures above 1 atm. This salt limit can be overcome by expanding the beam before passage through the salt window and this is indeed the preferred solution from a systems standpoint. However, in all these salt window configurations, the parasitic limit must be re-examined in light of the extra material surfaces at near normal incidence.

C. ELECTRODE LENGTH

The length of the PAM cavity parallel to the e-beam and the discharge electric field is determined primarily from voltage considerations. There are two voltages to be considered, the e-beam and sustainer discharge voltage. The e-beam voltage depends upon the foil thickness, cavity dimension and laser gas density. The cross sections involved are energy dependent and, hence, the calculation of voltage for required electrode separation is nonlinear. This subject is treated in detail in the e-beam section but for this discussion an approximate form will suffice. For voltages in excess of 200 kV, the cross sections flatten out and the stopping power of high energy electrons is $\sim 2 \times 10^6 \text{ V-cm}^2/\text{gm}$ for many materials, including aluminum, and CO_2 and N_2 . Thus, the voltage drop in the foil is $\sim 15 \text{ kV/mil}$ for aluminum and a few kilovolts per centimeter for N_2/CO_2 laser gas mixtures at one atmosphere. Therefore, a one-meter wide PAM would require e-beam voltages of $< 500 \text{ kV}$ at one atmosphere and as will be shown is less than that required for the sustainer discharge. Also, the sustainer electric field for those large devices accelerates the e-beam, thereby lowering the voltage requirements even further. The sustainer discharge electric field sets the voltage limit.

The optimum sustainer electric field for N_2/CO_2 laser gas mixtures in the range $0.2 < \psi_N < 0.5$ is in the range $\sim 9 < E/P < 13 \text{ kV}/(\text{cm-atm})$ for short pulses, i.e., $< \sim 5 \mu\text{s}$. An example of the e-beam and sustainer voltages required at one atmosphere for a sustainer electric field of 10 kV/cm is shown in Figure 45. Since both e-beam and sustainer voltages vary linearly with pressure (with the exception of the foil voltage drop for the e-beam) this plot is used to set the voltage requirements for all pressures.

In an analytical format, the anode-cathode dimension is given by

$$H = (E/P_c)^{-1} P_c^{-1} V_{\text{SUS}}$$

J1512

Figure 45 Voltage Requirements for Large Aperture PAM

where

E = the optimum sustainer discharge electric field

P_c = cavity pressure

V_{SUS} = sustainer discharge voltage

Since efficient energy transfer from a power supply requires a Pulse Forming Network, and since the delivered voltage from a PFN is twice the charge voltage (under matched conditions), twice the sustainer voltage must be handled in the system. With present technology, about 1 MV is the state of the art for matched load operation and, hence, 1 m anode-cathode dimensions (at 1 atm) are indicated. This estimate will change with future technology.

D. FLOW DIMENSION

The primary limitation on the PAM cavity dimension parallel to the laser gas flow depends upon the pulse repetition frequency, or PRF, of the laser system. For low frequency operation flow considerations play a minor role and the W dimension is set by outside factors such as the aspect ratio of the PAM aperture. At high PRF flow considerations such as pressure drops and acoustic clearing times limit the flow dimension.

At the low values of PRF, flow considerations are unimportant and the flow dimension can get large giving an aspect ratio of the aperture significantly larger than unity. A upper limit to the flow dimension occurs when the diagonal exceeds the parasitic limit. Before that usually occurs, large aspect ratio apertures would render mirror fabrication difficult. These considerations are externally specified and depend upon state of the art at any given time.

At higher PRF flow power and acoustic clearing time limit the flow dimension. According to a simple analysis, the flow velocity must be sufficiently high so that the transit time based upon flow velocity and cavity dimension is at least as short as the interpulse spacing, $(PRF)^{-1}$. Such a relationship gives rise to the analytic expressions

$$PRF \frac{W}{a} = M$$

where

a = sound speed

M = Mach number

However, the sudden addition of energy from the electrical discharge generates acoustic disturbances which travel with the sound speed (or slightly above in the case of shock waves). For acoustic disturbances propagating upstream, the waves "stand" in the flow if the flow speed is equal to the sound speed. The clearing time for the cavity is very long. The results of a detailed fluid dynamic description of this effect is shown in Figure 46 and described analytically by the expression

$$\text{PRF} \frac{2w}{a} = M (1-M)$$

The extra factor of two accounts for that space usually required for acoustic damping.

For Mach numbers $> 1/2$, the maximum flow dimension decreases and, thus $M = 1/2$ represents an upper limit for the design. However, the circulating power of the laser gas at a Mach number, M , is given

$$\frac{1}{2} \rho (Ma)^2 \text{ AREA}$$

whereas the laser output power is given by

$$P_{\text{LASER}} \propto \text{PRF AREA}$$

so that the ratio of circulating to laser output power varies as

$$\frac{P_{\text{CIRC}}}{P_{\text{LASER}}} \propto \frac{M^2}{1-M}$$

Since the electric power necessary to drive the laser gas flow loop can usually be expressed as a fixed fraction of the circulating power, system inefficiencies will grow for large Mach numbers. As a practical limit laser gas Mach numbers $> 1/4$ are difficult to achieve and lead to reductions in overall laser efficiencies.

H 6289

Figure 46 Acoustic Clearing Time in Flowing Cavities

V. PULSED POWER

A. INTRODUCTION

The CO₂ laser for this Laser Fusion Driver Study is an Electric Discharge Laser, and it is appropriate that the subject of pulsed (electric) power be a primary consideration in the overall design. There are two systems for the electric input energy, namely, the high energy e-beam and the high current sustainer discharge. The function of the e-beam is to provide a continued level of uniform ionization as a background for the sustainer discharge. The important issues for the generation of the e-beam are beam uniformity, foil lifetime high-voltage bushings and cathode technologies. The function of the sustainer discharge is to heat the electrons which are created by the e-beam to the optimum temperature ($T_e \cong 1$ eV) for efficient pumping of the laser gas mixture. For long pulse CO₂ lasers it is important that the sustainer discharge not create new electrons if arcing is to be avoided. For Laser Driver systems for which the electrical discharge pulse is likely to be $\lesssim 5$ μ s, it may be advantageous to run the discharge electric field at values for which some new electron production is possible. It is important that precise control over the discharge electric field and current be maintained for efficient operation.

A Pulse Forming Network (PFN) is essential for the sustainer discharge. The issues involved for the sustainer discharge PFN are current and voltage uniformity, high-voltage operation and the component technology associated with high voltage, high current and long life.

B. E-BEAM REQUIREMENTS

An input parameter to the kinetics code is the electron density which is determined by the e-beam current and energy. The required e-beam energy is determined by the stopping power of the foil and gas and the necessary uniformity of energy deposition. For uniform energy deposition, a fraction of the beam energy must be deposited in the opposite electrode, i.e., e-beam range is larger than the anode-cathode separation.

A plot of the stopping power for various materials is shown in Figure 47. For beam energies greater than ~ 200 kV most materials have a stopping power of $\sim 2 \times 10^6$ V/cm²/gm and the variation of stopping power with voltage is minimal. At

Figure 47 Stopping Power of Various Elements vs Electron Beam Energy

2 MV $\cdot$ cm²/gm, a one mil thickness of aluminum foil stops about 15 kV which represents a small fraction of the initial beam energy.

The stopping power in the gas at high beam energies is also $\sim 2 \times 10^6$ V $\cdot$ cm²/gm so that a (1:1) N₂:CO₂ laser gas mixture at 1 atm loses about 3 kV/cm or 300 kV/m. Thus, a 100 cm dimension parallel to the e-beam would require a beam energy of ~ 500 kV to maintain axial beam uniformity and stay on the flat portion of the stopping power curve.

A more accurate calculation based upon a Monte Carlo computer simulation of the scattering processes is shown in Figure 48. An important point exists here with regard to electron scattering. The results shown in Figure 48 pertain to a collimated beam where the transverse scattering and subsequent spreading has been suppressed. The method of suppression is the application of a magnetic field whose direction is parallel to the e-beam propagation and whose magnitude is of order of 1 kG. The purpose and method of generation of this guiding magnetic field will be discussed in more detail below.

The magnitude of the e-beam current determines the electron density. The reate equation for the electron density is

$$\frac{dn_e}{dt} = S - \alpha n_e^2 + \beta n_e$$

where

S = source function

$$= \frac{J_{eb}}{e} N_T \sum_i \frac{M_i}{E_i} \frac{\partial E}{\partial M_i}$$

J_{eb} = e-beam current density

N_T = total number density

M_i = mass of ith species

E_i = ionization of the ith species $\cong 35$ eV for N₂, CO₂

$\frac{\partial E}{\partial M_i}$ = stopping power of the ith species $\cong 2 \times 10^6$ V $\cdot$ cm²/g

α = recombination coefficient

β = attachment coefficient

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Figure 48 Maximum Scale Size for E-Beam Energy

The source function should be summed over the individual species but for N_2 , CO_2 mixtures, an average is acceptable. Solving for the steady-state electron density, a balance between the e-beam generation and the loss process of attachment and recombination is reached and given by

$$S = n_c^2 + \beta n_e$$

Solving for the e-beam current density,

$$J_{eb} = \frac{eE_i}{m} \left(\frac{\partial E}{\partial m} \right)^{-1} N_T \left[\alpha \left(\frac{n_e}{N_T} \right)^2 + \beta \left(\frac{n_e}{N_T} \right) \right]$$

where the attachment coefficient β is expressed as

$$\beta = \gamma \psi_c N_T$$

A plot of β for helium-free mixtures was generated from estimates of attachment cross sections and averaged over the electron distribution using the kinetics code and the results are shown in Figure 49. The relative importance of the recombination to attachment processes is seen by forming the ratio

$$\frac{\alpha (n_e/N_T)^2}{\gamma \psi_c (n_e/N_T)} = \frac{\alpha}{\gamma \psi_c} (n_e/N_T) = \frac{\alpha n_e}{\beta}$$

Since the recombination and attachment constants, α and γ depend upon E/P , the relative importance of these processes depends both on E/P and n_e/P . The quantity α is plotted in Figure 50 for N_2 and is fairly insensitive for E/N while the β constant is strongly dependent on E/N . Taking $\alpha \approx 10^{-7} \text{ cm}^3/\text{s}$ and n_e in the range 10^{12} - 10^{13} cm^{-3} , attachment is comparable to recombination for β in the range 10^5 - 10^6 s^{-1} for $3 \times 10^{-16} < E/N < 4 \times 10^{-16} \text{ V cm}^2$, respectively. Therefore, at the high E/P values for which the CO_2 short pulse system would run, both attachment and recombination play a role in determining the e-beam current. As an example of the order of magnitude of e-beam currents involved, assume that the contributions from recombination and attachment are equal and that the desired electron density is $3 \times 10^{12} \text{ cm}^{-3}$ at one atm. The required e-beam current density is

$$J_{eh} \approx \frac{2 e E_i}{m} \left(\frac{\partial E}{\partial m} \right)^{-1} \alpha n_c^2 N_T^{-1} \approx 100 \text{ mA/cm}^2$$

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Figure 49 Electron Attachment Rate Constants

Figure 50 Electron Recombination Rate Constants

C. E-BEAM APPARATUS

Electron emission occurs from either cold surfaces according to the field emission effect or on thermionic or heated surfaces via the Richardson-Dushman effect. Cold cathodes have several advantages in that they require no heater assemblies, have high emission current densities, i.e., $> 10 \text{ A/cm}^2$, and are quite rugged. These cathodes are not very susceptible to poisoning and will, therefore, generally survive a foil rupture. However, there are several disadvantages to cold cathodes which for the requirements of very long lifetime, may preclude their use. These disadvantages include diode closure effects which limit beam current at long pulse durations, limited lifetime, non-uniformly spaced emission sites and no simple method of grid switching.

Thermionic cathodes can give long lifetimes, with uniform output beams and can be grid switched to lessen the pulsed power problems. Further, thermionic cathodes can be used in conjunction with guide magnetic fields to overcome pinching limits which will be discussed in greater detail. The disadvantages of thermionic cathodes are that a separate heater power supply is required and may be poisoned by a foil rupture.

A cold cathode usually consists of a series of sharp spikes or thin blades of a high temperature material such as carbon, tantalum, tungsten, etc. The sharp features enhance the local electric field and electron emission occurs by field emission. As electron emission increases, the residual gas as well as outgas products from the cathode give rise to a plasmoid surrounding the tip. Electrons are pulled out of this plasma and accelerated toward the anode leaving the plasma positively charged. The accelerating electrons pull the plasma along with it in a manner analogous to ambipolar diffusion. This plasma closes towards the anode with a velocity of the order of

$$v_{\text{closure}} \cong \sqrt{KT_e/m_i}$$

where

T_e = electron temperature

m_i = mass of the lightest ions present, usually hydrogen

Typical closure velocities are of the order of a few centimeters per microsecond so that to prevent closure, the anode to cathode distance should be made as large as possible. The Child-Langmuir relationship between voltage and current; namely,

$$J_{eb} = 2.33 \times 10^{-6} V_{eb}^{3/2} / d^2 \text{ (A/cm}^2\text{)}$$

where

V_{eb} = accelerating voltage

d = anode-cathode distance

is appropriate for space-charge limited operation. If v_c is the electron closure velocity, the virtual anode-cathode distance is given by

$$d = d_0 - v_c t$$

where d_0 = initial anode-cathode distance. Combining the virtual anode-cathode distance with the Child-Langmuir law for space charge limited current and requiring that the e-beam current does not rise by more than twice its initial value, the plots of Figure 51 result. For a pulse length of 5 μ s and on accelerating voltage of 500 kV, the e-beam current density which results is of the order of 200 mA/cm². Although this value of J_{eb} is consistent with the order of magnitude of the required current density, the problem of diode closure is only marginally satisfied. Shorter pulse widths at higher values of electron density may be unattainable especially if the foil transparency is poor or the diode closure velocity increases.

Cold cathodes emit from points or in the case of flat plates or blades, from erosion sites which form at microscopic surface irregularities. At high current densities, $J_{eb} \gtrsim 10 \text{ A/cm}^2$, the spacing of these sites can be quite close giving rise to more uniform emission profiles. At low values of $J_{eb} \lesssim 1 \text{ A/cm}^2$, these emission sites tend to be as much as several centimeters apart giving nonuniform emission densities. The emission sites have a growth history in which they form out of an irregularity, grow as a result of electron emission and localized heating and decay from material erosion. This erosion limits the lifetime of particular emission sites and in general, decreases the lifetime of the cathode as a whole. Typical cold cathode lifetimes are less than 10^5 shots and a significant breakthrough is required for 10^8 shot lifetimes.

A new cold cathode technology has been recently developed at Sandia and AERL using carbon felt as the emission material. At low values of guide magnetic field, $\sim 1 \text{ kG}$, the closure velocity is reduced to $\sim 1 \text{ cm}/\mu\text{s}$, and as a result of the numerous

$$J_{eB} \sim v^{3/2}/d^2 \quad \text{CHILD LANGMUIR}$$

$$d = d_0 - v_e t \quad \text{CLOSURE}$$

$$J_{eBf} = 2J_{eB0}, \quad v_e = 3 \text{ cm}/\mu\text{sec}$$

Figure 51 Closure Effects for Cold Cathode Devices

emission sites in the felt material, the emission is uniform over large areas.(8) These cathodes are presently under test and evaluation and may be useful for CO₂ laser applications.

The application of an axial magnetic field is essential for both thermionic as well as cold cathodes as a method of overcoming the pinching limit. The pinching limit arises from the deflection of the high energy electrons in the self-magnetic field of the discharge current. (Pinching can also occur in the diode region as a result of the self magnetic of the e-beam current but this effect is negligible for the low values of e-beam current in CO₂ lasers.) The self-pinching results in a maximum aperture product in the following manner. The optimum discharge current return configuration, shown in Figure 52 results in a magnetic field that is zero along the center and increases to a maximum at each edge. The maximum value of B is given by

$$B = \mu_0 J_d W/2$$

where

μ_0 = permitivity of free space

J_d = discharge current (A/cm²)

W = height of the e-beam

The uniformity of energy deposition will be affected when the electron gyro radius of the edge beam electrons, r_g , is roughly twice the anode-cathode spacing, H, i.e.,

$$r_g = \frac{mv}{eB} \cong 2H$$

where

m = electron mass

v = (relativistic) electron velocity

Substituting the expression for the edge value of magnetic field, the current-aperture product is expressed as

$$J_d HW \lesssim mv/(e\mu_0)$$

$$\lesssim \frac{\beta}{(1-\beta^2)^{1/2}} \times 1.4 \text{ kA}$$

115-

$$I = j_s \frac{W}{2} (10^2) \text{ amp / meter ;}$$

$$j_s \text{ amp / cm}^2$$

$$W \text{ cm}$$

C9013

Figure 52 Optimum Discharge Return Current Configuration

where

$$\beta = \sqrt{\frac{T(T+2)}{(T+1)^2}}$$

T = electron energy normalized to rest mass, 511 meV

The current-aperture product is plotted vs electron energy in Figure 53. For a beam energy of 500 kV, the current density aperture product is limited to 2.4 kA which is roughly a factor of 10 below that needed for a large size PAM.

To overcome the current density aperture product limitation, a magnetic field can be externally applied parallel to the discharge current with a magnitude several times that of the self-magnetic field. There are two criteria for the strength of this external guide field, first, it must be several times as strong as the self field and second, the electron gyro radius about this field line must be small compared to the required medium homogeneity scale length to reduce electron scattering in the gas region. This latter restriction insures a collimated e-beam in the gas region, thereby, permitting uniform energy deposition. A nomograph permitting easy calculation of the required B-field and electron gyro radii (for relativistic velocities) is shown in Figure 54. For example, a discharge current of 2 A/cm² and an e-beam height, W = 100 cm gives rise to a self-magnetic field of ~130 G and an electron gyro radius at 500 kV of ~20 cm. If an external magnetic field of ~10³ G is applied along the discharge current corresponding to a JW of ~2000 A/cm, the gyro radius would reduce to a few centimeters at the same 500 kV.

The 1 kG magnetic field represents a small energy expenditure since the magnetic energy density

$$\frac{1}{2} B^2 / \mu_0 = 100 \text{ J/l or } 1 \text{ atm}$$

at B ≈ 5 kG. Thus a 1 kG field represents about 4 J/l which is significantly less than the electrically input energy density. Also, this energy is reactive and the only non-reactive energy associated with the B-field is that resistive loss in the coils which generate the field.

The thermionic cathodes traditionally used in CO₂ lasers were directly heated tungsten wires raised to temperatures in excess of 2500°C. There are major disadvantages with this type of thermionic cathode when used for large aperture, magnetic field guided devices. Since these wires act as discrete emitters,

G3081

$J_d HW > 10K$ AMPS

Figure 53. Current Density Aperture Product for Self Pinching

Figure 54 Nomograph for Calculation of Guide Magnetic Field

current nonuniformities will also exist in the laser gas volume and unless the density of these hot wire emission surfaces can be increased to approximate a near continuous source, their feasibility in large aperture lasers is questionable.

An alternate thermionic cathode is the dispenser type which was first developed for use in hydrogen thyratrons for radar applications and has been highly engineered for use in microwave tubes, cw ion lasers, etc. The three primary advantages of this type of thermionic cathode over tungsten filaments are lower temperature operation, long lifetime and broad area emission.

Low temperature operation results from the reduced work function of materials like elemental barium and tungsten. For example, the work function of pure tungsten is ~ 4.5 eV while a layer of barium on tungsten drops the work function to ~ 1.5 eV. Since the emission current density is exponential with the ratio of work function to cathode temperature, the required temperature for a given emission current density drops significantly as shown in Figure 55. The method of obtaining pure barium on a tungsten substrate with a carbonate salt of barium and then to apply heat in an evacuated environment. This "aging" or "conditioning" breaks the barium carbonate into carbon dioxide, barium tungstates and pure barium. Unwanted gases are removed by pumping and the result is a replenishable layer of pure barium on the tungsten surface. Through careful control of the surface temperature and adequate barium carbonate reserve, sufficient barium can be dispersed to provide very long cathode life at high current density as indicated by Figure 56. The heater power for these low temperature cathodes is also much lower than for pure tungsten as shown by the design nomograph of Figure 57. Only ~ 1.6 W/cm² are required for 100 mA/cm² operation. These dispenser cathode systems are under active development for visible lasers and high current densities > 10 A/cm² have been produced for times > 5 μ s.

Dispenser cathodes will provide a broad area of uniform emission and will be amenable to use with guide magnetic fields. An example of how the guide magnetic fields may also be used to implement non-intercepting grid and foil support structures is shown in Figure 58. Non-emitting regions of the dispenser cathode shield the grid wires and foil support structure so that little or no electron current is lost to those structures.

The design of the thin foil which separates the vacuum from the laser gas region is critical for repetitively pulsed operation. The basic configuration is shown in Figure 59. There are two limits to the foil design, a single-pulse limit based upon total Coulomb transfer and a repetitively pulsed limit based upon heat conduction to the cooling structure.

Figure 55 Current Densities from Various Thermionic Emitters

Figure 56 Lifetime Estimates of Dispenser Cathode Structures

J1500

Figure 57 Heater Power Consumption for Dispenser Cathodes

H4536-1

Figure 58 Non-Intercepting Grid and Foil Support Structures with Guide Magnetic Fields

HI666

Figure 59 Design Concept for Electron-Beam Foil

The single-pulse limit results from a balance of electron energy deposited in the foil to the bulk temperature rise in the following manner:

$$J_{eb} S \tau_p = \rho C_p \Delta T$$

where

S = stopping power

$$\cong (2 \times 10^6 \text{ V cm}^2/\text{g}) \rho \delta \text{ for } V_{eb} > 300 \text{ kV}$$

ρ = density of the foil

δ = foil thickness

C_p = specific heat $\cong 1 \text{ J } ^\circ\text{C}^{-1} \text{ g}^{-1}$

ΔT = temperature rise in the foil

A plot of J_{eb} vs τ_p is shown in Figure 60 as the line marked "single-pulse limit." For the parameters of interest for Fusion devices, i.e., $J_{eb} \lesssim 100 \text{ mA/cm}^2$ and $\tau_p \cong 5 \text{ } \mu\text{s}$. The total coulomb transfer is well below the melting point limit and is of no concern.

The calculation for repetitively pulsed operation is outlined in Figure 61. The first e-beam pulse deposits a fraction of its energy and raises the foil temperature to T_0 . During the interpulse time, the heat is conducted through the foil to the foil support structure and the foil temperature (at the mid-point between the support structures) decreases to a fraction, β , of its initial temperature. The process repeats but converges to a steady-state temperature. The limits on repetition rate and Coulomb transfer to raise the foil temperature to the maximum value are shown in Figure 62. It is clear that for the modest repetition rates of interest, i.e., $\lesssim 50 \text{ Hz}$, there is no design limitation with the foil for either single- or repetitively-pulsed operation.

D. DISCHARGE PARAMETERS

The parameters which size the pulsed power requirements for the PAM are the anode-cathode voltage, the total sustainer current, discharge impedance and pulse duration. The discharge voltage is fixed by the product of optimum electric field and anode-cathode distance, i.e.,

$$V_{SUS} = (E/P_c)_{OPT} P_c H$$

Figure 60 Heating Limits for Electron-Beam Foils

H1671

Figure 61 Heating Model for Foils Under Repetitively Pulsed Operation

HI663

Figure 62 Steady State Heating Limits for Electron-Beam Foils

The total sustainer current is given by the product of sustainer current density and aperture, i.e.,

$$I_{\text{SUS}} = J_{\text{SUS}} W L_c$$

where the sustainer current density is given by

$$J_{\text{SUS}} = e n_e V_d (E/P)$$

where V_d = drift velocity which is - E/P dependent. The drift velocity is an output from the kinetics code and for various laser gas mixtures is plotted in Figure 63. As a estimate, consider a (1:1) laser gas mixture at an electron density of $3 \times 10^{12} \text{ cm}^{-3}$ to give a sustainer current density of $\sim 3 \text{ A/cm}^2$ with W and L_c equal to 2 and 3 m, respectively, a total sustainer current of $\sim 200 \text{ kA}$ is obtained. In this example, the discharge impedance for a 1 mV sustainer voltage is $\sim 5 \Omega$. The pumping pulse duration assuming a 100 kJ output from the PAM and a 15% intrinsic efficiency would be

$$\tau_p = E_{\text{OUT}}^{-1} V_{\text{SUS}} I_{\text{SUS}}^{-1}$$

or

$$\tau_p \cong 3 \mu\text{s}$$

In actual fact the electrical pumping pulse duration as well as the sustainer current density are given as an output of the kinetics code but estimates such as this serve as a consistency check.

One design limit which is an important constraint for high electron densities and short pumping pulse durations is the exclusion of current from the volume of the discharge, i.e., the skin effect. At high frequencies, the time varying magnetic field cannot penetrate an ionized medium rapidly and the discharge current will flow around the periphery of the discharge rather than volumetrically through it. The depth of penetration of the current pulse is given by

$$\delta = (\omega \mu_0 \sigma)^{-1/2}$$

J2269

Figure 63 Drift Velocity in CO_2 Laser Gas Mixtures

where

$$\omega \approx \frac{\pi}{2} \frac{1}{t_R}$$

t_R = risetime of the current pulse

μ_0 = permittivity of free space

σ = conductivity of the ionized medium

The conductivity of the ionized medium is given by

$$\sigma = J_{SUS}/E = en_e v_d/D$$

A plot of skin depth vs sustainer current risetime for an electric field of 10 kV/cm and drift velocity of 10 cm/ μ s is shown in Figure 64. For risetimes of 1 μ s and electron densities of 10^{13} cm⁻³, the device size is limited to ~ 100 cm. These values of n_e and t_R are extreme and it is not anticipated that skin depth will be a significant problem.

E. DESIGN OF PULSE FORMING NETWORK

The choice of PFN type and specific design and placement of the components depend critically on the sustainer discharge parameters the majority of which are outputs of the kinetics code. It will be clear that as a result of factors such as stray inductance, voltage levels and lifetime estimates of switchgear, that laser configurations which result in long pump pulse durations and high discharge impedance levels will be feasible with the present state of the art or with near term development. Short pumping pulses at low discharge impedance levels will be shown to be difficult or very expensive for the lifetimes required and will require a fundamental breakthrough in device technology for their implementation.

A Pulse Forming Network is a configuration of lumped elements, capacitors and inductors, which can deliver a nearly constant voltage and current to a (fixed) load impedance for a finite pulse duration. There are many different PFN configurations each of which is the dual (with respect to terminal characteristics) to a transmission line of constant impedance. Each PFN type has its own set of advantages and disadvantages and is suited to particular applications. The E-type, for example, consists of identical capacitors and inductors and can easily accommodate variable pulse duration. The C-type makes the best use of stray inductance and is generally favored for

H5350

Figure 64 Skin Depth Limitations in Large Aperture Lasers

short duration PFNs. Other variations such as the A-type use uncharged elements to sharpen rise and fall time and so on.

To understand the effect of discharge parameters upon the choice and design of a PFN, the direct coupled E-type is most useful. The term "direct coupling" means that the PFN is connected directly to the discharge load with no voltage transformation as would be the case with a transformer coupled PFN. The advantages of a transformer coupled PFN in terms of switch-gear and high voltages will be outlined later.

The basic configuration of an E-type PFN is shown in Figure 65 the architecture of the PFN is characterized by three dimensions, M, N and S. The M dimensions pertains to the voltage level, the N to the shape or constancy of the voltage and current, and the S to the impednace level of the load. For components, i.e., "switches" capacitors and indicators, which are rated at voltage levels of V_C , the total charge voltage is given by

$$V_O = MV_C$$

The maximum value of capacitor voltage consistent with low stray inductance and long component lifetime is presently ~ 100 kV. Above 100 kV internal breakdown by corona discharges limits the lifetime and leads to premature failure. For similar reasons, ~ 100 kV is presently the state of the art for long life switch-gear. At 100 kV per stage, 20 stages would be needed to charge an E-type PFN to the 2 MV required for a 1 MV sustainer discharge. (For a maximum power transfer, the PFN should be terminated in its characteristic impedance, and hence, half the charge voltage appears across the internal source impedance while the other half appears across the load.)

For a squared pulse shape with a 10% rise and 20% fall time, at least five sections are required, i.e., $N \geq 5$. The equation for the pulse length is given by

$$t_p = 2N \sqrt{Lc/M}$$

where C = capacitance of an individual capacitor rated at MV, the impedance level of the entire PFN is specified by the S dimension and is given by

$$Z = \frac{1}{S} \sqrt{LM/C}$$

so that the impednace level of an individual string is $\sqrt{LM/C}$ and the total impedance is the parallel combination of S strings.

H8629

Figure 65 E-Type PFN

This analysis for the E-type PFN is valid as long as the sum of the stray inductances in each leg is much less than the series inductor, i.e.,

$$L \gg ML_S$$

where

L_S = internal inductance of the capacitor plus and switch inductance.

Typical values of the stray inductances of long lifetime repetitively pulsed capacitors is ~ 40 nH. Similarly the stray inductance of spark gaps is of the same order, 40 nH, when the internal inductance and lead or connector contribution is accounted for. In this estimate, esoteric low inductance capacitors are not considered because they generally are not capable of being repetitively pulsed, are not reliable and do not possess long lifetime capabilities.

These E-type PFN relations can be summarized as follows:

$$\begin{aligned} (SZ)t_p &= 2 NL = 10 L \\ (SZ)^{-1}t_p &= 2 NC/M = C/2 \end{aligned}$$

where

$$N = 5$$

$$M = 20$$

These equations are plotted in Figure 66 for the pulse time, t_p , vs string impedance, SZ , for various values of inductors and capacitors. When the series inductor, L , becomes comparable to the sum of the stray inductances in the string, the analysis is not valid and it is customary to switch to an alternate type PFN in which the stray inductance is utilized as part of the circuit. Such a PFN circuit is discussed shortly. The stray inductance limit delineates the map into a zone where E-type PFN is practical and a zone where other types such as C-types are required. The line of demarcation for the PFN types is taken as

$$L = 5 \times \Sigma L_S = 10 \mu H$$

J1508

Figure 66 E-Type PFN Design Map

The value of load impedance is specified from the kinetics code in conjunction with the PAM dimensions but the maximum and minimum values of the number of strings is somewhat arbitrary. Two criteria help to define the limits on S; namely, the physical size of the strings and the inductance associated with the string-bushing combination. To keep the load inductance from becoming too large, the minimum number of strings would be that required to give a bushing which feeds a squared geometry as illustrated in Figure 67. The maximum number of strings is also limited by inductance and strings should not be placed too far outboard too far from the cavity.

As an illustrative example of how the kinetics output influences the choice of PFN design, a specific example is chosen. Consider the kinetics output for a (1:1) mix of $N_2:CO_2$ at 1/2 atm and an electron density of $0.5 \times 10^{12} \text{ cm}^{-3}$. The kinetics code output for this case as tabulated in Section III is

$$\begin{aligned} g_o &= 2.1\% \text{ cm}^{-1} \\ E &= 6.2 \text{ kV/cm} \\ J_{\text{SUS}} &= 0.8 \text{ A/cm}^2 \\ t_p &= 6.8 \text{ } \mu\text{s} \end{aligned}$$

For a maximum

$$(g_o L_c)_{\text{MAX}} = 9$$

The gain length is $L_c = 4.3 \text{ m}$. The anode-cathode dimension for a 1 MV sustainer voltage is 1.6 m. At low PRF, i.e., less than 10 Hz, the length in the flow direction, w, is not constrained by Mach number effects and for convenience, a square output operative is chosen, i.e., $W = H = 1.6 \text{ m}$. The minimum number of bushings would be

$$S_{\text{MIN}} \cong L_c / W \cong 3$$

the load impedance, is given by

$$Z = EH / (J_{\text{SUS}} L_c W) = \frac{E}{J_{\text{SUS}}} \cdot \frac{1}{L_c}$$

$$18 \cong \Omega$$

J2270

Figure 67 Bushing Geometry

so that the minimum value of $SZ = 54 \Omega$. The operating point $SZ = 500 \Omega$, $t_p = 6.8 \mu s$ lies in the E-type region.

On the other hand, an operating point of (1:1) mix at 1/2 atm and $n_e = 3 \times 10^{12} \text{ cm}^{-3}$ gives an almost cubic PAM cavity, i.e.,

$$L_c = 9/g_o = 9/4/4\% \text{ cm}^{-1} \cong 2 \text{ m}$$

$$H = 1 \text{ MV}/6.2 \text{ kV/cm} = 1.6 \text{ m}$$

$$W = H = 1.6 \text{ m}$$

$$t_p = 2.3 \mu s$$

$$Z = E/(J_{SUS} L_c) = 6 \Omega$$

$$S_{MIN} \cong L_c/W = 2$$

This operating point of $(SZ)_{MIN} = 12 \Omega$ and $t_p = 2.3 \mu s$ is outside the E-type region and the number of strings must be increased to $\sim SZ \sim 40$ or about 7 strings. Estimating the width of the PFN is $\sim 3.5\text{--}4 \text{ m}$ and hence, the PFN would significantly overhang the PAM cavity.

An estimate of the minimum risetime of the cavity is instructive. For the first example where $t = 6.8 \mu s$, a 10% risetime would be 680 ns provided that the total L/Z time constant (where L = cavity inductance) is not a constraint. The cavity inductance is estimated as

$$L_{CAVITY} \cong \mu_o WH/L_c$$

Inductance of the bushing and connecting leads is typically a factor of 10 larger than this estimate so that the minimum cavity risetime is of the order of

$$t_R \gtrsim 10 \mu_o WH/(L_c Z)$$

For the first example, the minimum risetime is of the order

$$t_R \gtrsim 400 \text{ ns}$$

When the pulselength becomes short and the load independence drops as is the case for high cavity pressures and high electron densities, the stray inductance of the capacitors, switches, leads, etc., became comparable to the series inductor and the analysis for E-type PFNs breaks down. In this case, one switches to a type of PFN in which the stray inductance is used in conjunction with an external inductor to resonate with the capacitance giving the desired high frequency (short use time) response. The schematic of a typical C-type PFN is shown in Figure 68. Again as in the case of the E-type, there are three parameters which defines the matrix, the M dimension for stacking of capacitors to achieve the desired charge voltage, the N dimension to control rise and fall time characteristics, and the S dimension to match the load impedance. Unlike the E-type in which the pulse time was governed by the number of sections, N, and for which the pulse could be lengthened or shortened by adding or subtracting sections, the pulsewidth of the C-type is determined by the fundamental tank circuit, i.e.,

$$\omega_0 \cong \pi/t_p = (L_1 C_1)^{-1/2}$$

as shown in Figure 69. The bulk of the energy stored is also in this fundamental tank circuit. The higher (odd) harmonics add the high frequency response and give the desired rise- and fall-time characteristics.

The charge voltage is given by the number of M sections,

$$V_0 = M V_C$$

the pulse duration is given by the fundamental frequency

$$t_p \cong \pi \sqrt{L_1 C_1} / M$$

and approximately N = 4 sections are needed, i.e., up to the third harmonic, to give a 10% rise- and 20% fall-time for the pulse. The impedance level for the 4-section line can be expressed in terms of the fundamental tank circuit as

$$Z = \frac{1}{S} \frac{\pi}{2} f \sqrt{L_1 M / C_1}$$

where $f \cong 88\%$. This analysis is valid as long as the required inductance in a leg is not smaller than the sum of the stray inductance in that leg. In this way, the stray inductance is added to and becomes part of the inductance needed for the resonant tank circuit.

J1535

Figure 68 C-Type PFN

J1509

Figure 69 Harmonics in the C-Type PFN

The combined equations for C-type PFNs are

$$(SZ) t_p = \frac{\pi}{2} f L_1$$

$$(SZ)^{-1} t_p = \frac{2}{f} G/M$$

and these are plotted in Figure 70 for $M = 20$ and $f = 88\%$.

As an example of other operating lines which can be placed in this figure, the operating load line corresponding to a 1 MJ laser output at 10% intrinsic efficiency utilizing 15 PAM and 5 strings per PAM is plotted on Figure 70. The product of pulse time and total impedance level is given by

$$\frac{V^2}{(SZ)} t_p = E_{OUT} \eta^{-1} (15 \text{ PAMs})^{-1} (5 \text{ Strings})^{-1}$$

where t_p is expressed in microseconds. This load line is drawn on Figure 70 so that a choice of either PAM load impedance or pulse duration automatically specifies the other. For example, the 2.3 μs pulse duration of the previous example required a load impedance of $\sim 6 \Omega$ or an SZ product of ~ 30 . Locating $t_p = 2.3 \mu s$ and $SZ = 30$ on the C-type map, the operating point is close to but to the right of the load line and is, therefore, an acceptable design for a C-type PFN, notwithstanding other problems associated with components such as switches, etc.

In summary, although there are several types of PFN circuits, the E- and C-types are sufficient to characterize the long- and short-pulse duration applications, respectively. For $t_p \gtrsim 4 \mu s$, E-type PFN appear feasible for the load impedances and numbers of PAMs and strings of interest. Below 4 μs , C-type PFNs must be used but it is not feasible to drop the pulse duration much below 1-2 μs . The following discussion concerning switch technology will place more stringent restrictions on the lower limit of acceptable pulse durations and load impedances.

There are two general methods of connecting the PFN to the load, either directly or indirectly with a (step-up) transformer. For low inductance and short-pulse duration cases, the direct method is the only possibility because of the high leakage inductance of the pulse transformer, usually of the order of microhenries. The primary benefit to be derived from transformer coupled operation is that high charging voltages are not required and, hence, simple switchgear can be used. For example, using a 10:1 step-up transformer similar in design to the equipment presently in use for high-power CO₂ lasers for DOD applications,

Figure 70 C-Type PFN Design Map

the charge voltage for a 1 MV pulse need only be 200 kV. One or two stages of series capacitance would be required and appropriate hydrogen thyatron switchgear already exists. Switchgear lifetime will be the major component of total pulsed power costs and, hence PAM designs which can accommodate long pumping pulse durations at high load impedances are desirable.

F. PULSED POWER SWITCHGEAR

There are basically three types of switchgear presently used for high-voltage, high current, repetitively pulsed power systems; mainly, those using thermionic cathodes (thyatrons), field emission cathodes (spark gaps) and liquid metal cathodes (ignitrons). All three are gas discharge tubes since vacuum tubes, although very desirable in their controllability, are not generally capable of high current operation. The cold cathode devices, both spark gaps and ignitions are basically arc discharges in which significant electrode erosion occurs. Electrode erosion is precisely the reason liquid metal cathode (ignitions) were developed as a means of supplying a replenishable cathode for the device. Thermionic cathode devices operate in a stable discharge mode with little electrode erosion and the current is supplied by oxide or dispenser cathodes similar to those discussed for e-beam cathodes. The question of electrode erosion is the single most important factor in determining switchgear lifetime.

Thyatron lifetime is determined by the reserve quantity of barium salts stored in the dispenser cathode. As the heating rate increases to provide higher current densities, the evaporation rate increases and lifetime decreases unless special provision is made in the cathode design to store more barium material. As an example of the inherent lifetime of dispenser cathode materials at the 10 A/cm^2 level which is characteristic of thyatron design, the data of Figure 71 is shown. Inherent lifetimes of 10^6 hr are possible. Although the primary factor in cathode lifetime is the "on-time" of the cathode, the number of shots, time at which the anode-cathode voltage is kept on, heater design, etc., are also contributing factors to decreased lifetime. Ion bombardment at high voltages can poison dispenser cathodes and arcing during the pulse at high peak current operation is damaging to these cathodes. However, commercially available thyatrons with cathode lifetimes of 1000 hr (corresponding to shot lifetimes in excess of 10^9 pulses) are available and special designs for weather radar applications with 10,000 hr cathode lifetime are in use. There appears to be no fundamental limitation to extending cathode lifetime to their full potential with proper development efforts.

BARIUM / STRONTIUM - TUNGSTEN / NICKEL

Figure 71 Lifetime Data for Dispenser Cathodes

Other characteristics of hydrogen thyratrons are also very attractive for Laser Fusion Driver applications. For example, the peak voltage of a newly developed thyratron (EGG-MAP 250) shown in two versions in Figure 72 is 200 kV at about 1 MW average power handling. The taller version is air operated and the shorter must be oil immersed to provide adequate external voltage standoff. The peak currents switched by these tubes are in excess of 10 KA and designs are available to raise the peak current capabilities to the 40 KA level. Again there appears to be no fundamental scaling limit to peak current capability, the major limitation being the diameter of the ceramic envelope currently commercially available.

The grid pulse required to drive these large high-voltage tubes is also in the 10 kV range and easily deliverable with smaller thyratrons and pulse transformers. A major drawback to these tubes, the requirement to supply low-voltage heater and hydrogen reservoir supply current, especially troublesome in applications where the cathode of the tube is not at ground potential, may be overcome. New thyratron designs⁽⁹⁾ utilizing the heat generated in the tube as a result of anode dissipation has been used to eliminate these low-voltage supplies. In these instances, there appears to be very little distinction between the terminal characteristics of thyratrons and spark gaps. Any advantage of spark gaps by virtue of lower cost per unit may be completely reversed by lifetime considerations.

Spark gaps are gas filled devices with field emitting electrodes usually fabricated out of a high temperature material with a low sputtering rate such as tungsten. Gaps may be operated in a triggered mode or may be overvolted and for this reason, spark gaps are the appropriate switch for MARX bnak applications. The state of the art for spark gap performance is ~ 100 kV hold-off (in repetitively pulsed operation) with maximum repetition rates of the order of 100 Hz and maximum Coulomb transfer of ~ 1/2 Coulomb per pulse.

Electrode erosion limits spark gap lifetime and the typical erosion rate is measured to be several tens of micrograms per Coulomb transferred. In this sense, spark gaps are "Coulomb counters" and their lifetime is directly related to the number of shots. These erosion rates correspond to maximum Coulomb transfers of 10^4 - 10^5 Coulomb/gram of electrode material removed. Spark gap become unreliable when a fraction of a gram of material has been lost corresponding to total Coulomb transfer of 10,000 Coulombs. Many suggestions have been made to provide a replenishable electrode structure which can be advanced in the manner of carbon arc electrodes for high-power lighting arcs, but this suggestion has not be verified. For large electrodes, a fraction of a gram often corresponds to less than a 5% change in gap

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Figure 72 250 kV Thyratrons. The taller tube is operated in air, the shorter tube in oil. Courtesy of EGG Corporation, Bedford, Mass.

spacing so that simply supplying more electrode material does not appear to be the correct solution. The problem may be more closely correlated with the surface smoothness of the electrodes. It is often found that polishing the electrodes restores reliable operation. An ignitron with liquid metal electrodes is an attempt to remedy this surface erosion problem. In this device the surface always maintains its smoothness.

A scaling law⁽¹⁰⁾ for electrode erosion as a function of current is indicated in Figure 73. This scaling law has not been verified for wide classes of tungsten electrode materials and low values of electrode current. If this scaling law is valid at low currents, then increased gap lifetimes are possible since for fixed pulse duration the erosion rate drops slower than the peak current. An example of the lifetime increase according to such a scaling law is shown in Figure 73. A data point⁽¹⁰⁾ for a scintered copper-tungsten electrode material, i.e., $\sim 75 \mu\text{g/Coulomb}$ at 2000 KA peak current, was used to generate the proposed scaling law. If the scaling law is valid at peak currents as low as 20 KA, very low erosion rates, $\sim 2 \mu\text{g/Coulomb}$ are predicted and long life will result. However, industry data on this material as well as other copper-tungsten complexes indicate that erosion rates lower than 10-20 $\mu\text{g/Coulomb}$ are not observed. Such industrial data corresponds to 50,000-100,000 Coulombs of total charge transfer.

The scaling law depicted in Figure 73 can be used to generate maps for the number of switches required as a function of various series and parallel stacking configurations. For example, if it is assumed that a single spark gap switch can hold off the full charge voltage, say 2 MV, so that only one series gap be used, the number of gaps required for one year at 10 Hz PRF and 1 MJ laser output is $\sim 6 \times 10^4$ gaps. At a charge voltage of 2 MV and laser efficiency of 10%, the number of gaps is:

$$\begin{aligned} \text{Number of gaps} &= \frac{1 \text{ MJ} \times^{-1} \times (V_o/2)^{-1} (3 \times 10^8 \text{ shots/year})}{5 \times 10^4 \text{ Coulomb/gap}} \\ &= 6 \times 10^4 \text{ gaps.} \end{aligned}$$

Since the number of switches can always be traded off against the number of times the switch must be replaced per year, the curves shown in Figures 74(a-c) can be generated. In drawing these curves, the number of switches in parallel has been varied to change the peak current and the scaling law shown in Figure 73 was used to compute the total Coulomb transfer. The shaded areas represent present state of the art and the remaining lines indicate the benefit which could be obtained if a significant breakthrough in the understanding of electrode erosion were made.

J1532

Figure 73 Electrode Erosion Scaling Laws

Figure 74(a) Spark Gap Tradeoff Curves 2 MV Switch

Figure 74(b) Spark Gap Tradeoff Curves 200 kV Switch

Figure 74(c) Spark Gap Tradeoff Curves 100 kV Switch

The impact of limited lifetime of spark gap switches on the total cost of the sustainer discharge PFN was compared for the direct coupled case (using spark gaps) and the transformer coupled case using thyratrons. The unit cost of the spark gap was increased to reflect the expense involved in rendering the switch easily refurbishable. Capital cost was estimated at \$500/gap with a 10% refurbishing cost. The cost comparison for the two systems is shown in Figure 75.

Ignitrons would appear to be a compromise between the cold cathode spark gap and thermionic-cathode thyratrons. Ignitrons have long been used in the power industry for moderate voltage, high average current switchgear and a considerable body of empirical data exists for these devices. The lifetime limitations are connected with the igniter circuit which triggers the tube. The maximum Coulomb transfer is appreciable as is the peak current rating but unless a significant redesign is achieved, only low pulse repetition rates are possible. A typical industrial ignitron has the following rating:

$$V_{MAX} = 25 \text{ KV}$$

$$(I_{pk})_{MAX} = 300 \text{ KA}$$

$$(Q_{pulse})_{MAX} = 200 \text{ Coulombs}$$

$$(I_{AV})_{MAX} = 10 \text{ A}$$

The lifetime at these rated values is $\sim 10^3$ shots but an empirically determined lifetime scaling law is

$$\text{Lifetime} \propto (I_{pk})^{-3} (t_p)^{-2} (V)^{-2}$$

Thus, the lifetime of the tube can be increased by suitably de-rating any or all of the tube parameters. For the industrial tube described above, the lifetime relationship becomes

$$\text{Lifetime} = 10^3 \left(\frac{300 \text{ KA}}{I_{pk}} \right)^3 \left[\frac{200 \text{ Coulombs}}{(300 \text{ KA}) t_p} \right]^2$$

where it is assumed that the ignitron voltage has been left unchanged and

$$I_{pk} = J_{SUS} L_c W/S$$

Figure 75 Cost Tradeoff for Direct and Transformer Coupled Pulsed Power Systems

Also, to keep the average current in the ignitron below maximum rating, it is required that

$$I_{AV} = I_{pk} t_p PRF < 10 A$$

although for this application, the average power restriction is not exceeded. The total number of ignitrons required for one-year operation is then

$$N(IG) = N(PAM) \times S \times \frac{V_o}{25 \text{ KV}} \times \frac{3 \times 10^7 \times PRF}{\text{Lifetime}}$$

where $N(PAM)$ = number of PAM

S = number of strings

V_o = total charging voltage

The number of ignitrons required for a 1 MJ laser output with 20 PAM and 5 PFN strings/PAM, charge voltage of 2 MV and PRF of 10 Hz is estimated as an example. The lifetime of each tube is

$$\text{Lifetime} = 10^3 \left(\frac{300 \text{ KA}}{2 \text{ KA}} \right)^3 \left(\frac{200 \text{ Coulombs}}{300 \text{ KA} \times 5 \mu\text{s}} \right)^2 \gg 10^8$$

since the peak current and pulse time have been derated by large amounts. The peak current is:

$$I_{pk} = (1A/cm^2)(10^4 \text{ cm}^2)/5 \text{ strings} = 2 \text{ KA.}$$

Since the lifetime is much longer than 10^8 shots, the number of ignitrons required is simply

$$N(IG) = 20 \text{ PAM} \times 5 \text{ strings} \times \frac{2 \text{ MV}}{25 \text{ KV}} = 8000$$

which at a few million dollars each is quite reasonable. However, there are major uncertainties which must be resolved including the lifetime, repetitively pulsed operation and stackability in voltage. Although the lifetime is projected by the scaling law to be far in excess of 10^8 shots, ignitrons are not generally operated in this mode and such lifetime predictions must be verified. Ignitrons are also not generally repetitively pulsed and this feature must be tested to insure controlled condensation of the mercury. Significant redesign may be necessary to achieve

repped operation. Finally and perhaps most important, since the standoff voltage capability is low for ignitrons, they must be stacked by themselves or in a MARX configuration to achieve megavolt operation. Ignitrons have not been tested in this mode and it is not clear as to whether or not such tubes can be operated in an overvolted mode. If not, than all ignitrons in the stack will have to be triggered with a stringent jitter specification.

VI. AERODYNAMIC AND MATERIAL WINDOWS

A. INTRODUCTION

The gas pressure in the PAM cavity will always be significantly higher than the pressure in the target chamber and therefore a pressure interface between the two systems is required. Another requirement is that there be some shielding between the target and the last optical element to protect its surface from soft x-rays which are produced by the pellet. A convenient method of stopping soft x-rays is to use a gas absorber so that there are two pressure interfaces for a Laser Fusion Driver. The first interface is between the PAM cavity pressure and the intermediate pressure at the last optic and the second interface is between the pressure at the last optic to the relatively hard vacuum of the target chamber.

There are two options for the design of the pressure interfaces, an aerodynamic window (A/W) or a material window using a transparent material such as NaCl. There are several advantages and disadvantages for both types of windows and the following sections will outline these issues as well as discuss their impact on the remainder of the system.

Aerowindows have been used on CO₂ laser for other applications but their design has always been optimized for low size, weight and consumables, since these designs are for open-cycle airborne use. The A/W for a Laser Fusion Driver would probably be closed-cycle with size and weight not a major issue. The major issues are the electrical power consumption (which affects the overall system efficiency) and the optical distortions of a beam passing through the A/W.

Salt windows have also been extensively used in high-power CO₂ lasers. The principal issues concerning their use are lifetime and maximum allowable fluence loading for short pulse applications. Also the maximum diameter of salt windows, which results from fabrication limits, may affect the design of the entire system.

B. DESIGN OF AEROWINDOWS

There are several different types of A/W utilizing subsonic and supersonic flow patterns as well as supersonic vortex flow. These flow configurations have the one common feature that pressure gradients are balanced by the centrifugal force associated

with curved flow as illustrated schematically in Figure 76. For subsonic flow, the pressure balance is simply

$$\Delta P = \rho v^2 / R$$

where R is the local radius of curvature of the flow. As the flow velocity is increased beyond the sonic point, the continuous turning aspect becomes discontinuous. In these cases, the pressure gradient is balanced either by shock waves or Prandl-Meyer expansion waves. The air flow in an A/W need not be moving in the Lab frame. For example, the rotor blades of an axial compressor turn the flow by virtue of their air foil shape and angle-of-attack. The curved flow is balanced by the axial pressure gradient. An axial compressor run in the stalled mode can be used as an A/W in much the same manner as a turbomolecular pump can evacuate a closed volume to a low pressure. These concepts will be developed in more detail in the following sections.

Historically, subsonic flows have been used for low pressure drops (Aero Curtains) where the pressure drop may only be a fraction of a psi. Such windows find application in the interface between an atmospheric laser cavity and the ambient atmosphere where the laser cavity is run just above 1 atm for reasons flow control and freedom from contamination. Aero-windows which must stand off large pressure differences are generally supersonic because of the higher energy content of the flow. Examples of supersonic A/W are those used on Gas-dynamic Lasers (CO₂) run at 1 atm exhausting into the air at high altitudes. The requirement to keep these A/W small and to limit the amount of consumable gas leads to the use high Mach number flows, i.e., $M = 3-4$. These flows are run open-cycle and no attempt is made to recover the pressure drop with a diffuser or to recover the gas. Such open-cycle operation is impractical for a Laser Fusion Driver.

Closed-cycle operation is practical for a continuously run Laser Fusion Driver, and hence past analyses must be re-optimized in favor of low power consumption. In the following analysis, the fluid mechanical expressions, although straightforward, are tedious to derive and since their derivation is well referenced in the literature,⁽¹¹⁾ the equations will be given in outline form without derivation.

There are three basic configurations of supersonic A/W as shown in Figure 77; the (a) expansion, (b) compression and (c) shock-expansion types, depending upon whether the pressure drop is taken across a compression shock, expansion waves or a combination of an expansion and a shock wave respectively.

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Figure 76 Curved Flow Patterns of an Aerodynamic Window

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Figure 77 Supersonic Aerowindow Configurations:
(a) expansion, (b) compression, (c) Shock-expansion

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Although the compression window generally gives the lowest mass flow for a given pressure standoff, the successful operation of this window requires complicated boundary layer control at the exit nozzle due to the presence of unfavorable pressure gradients there. The expansion window requires somewhat higher mass flow but the boundary layer will not separate at the nozzle exit because the pressure is decreasing. The shock-expansion window requires somewhat less mass flow than the pure expansion window but the optical quality of this window may not be easily analyzed since the beam must pass through an interacting shock-expansion wave system.

High Mach number flow in a closed-cycle flow system will require tremendous electrical power to drive the flow unless a significant breakthrough is made in the design of high efficiency diffusers. Such diffusers do not presently exist. Therefore the Mach number should be kept as low as possible to reduce shock losses. With low Mach number flow, the pressure standoff capability of a single A/W is small and hence several stages of A/W must be placed in series to achieve the total required pressure drop. A schematic diagram of a staged expansion A/W is shown in Figure 78. Gas enters the first stage of the A/W at the high pressure side at the plenum pressure which is the PAM cavity pressure. The gas is expanded in the supersonic nozzle and the flow is turned in the window aperture where it is balanced by the (small) pressure gradient. The flow is shocked back to subsonic conditions in the exhaust duct with little recovery of shock losses. The subsonic flow is then cooled and smoothed to remove density and thermal gradients and fed in a series manner to the inlet nozzle of the second stage of the A/W. The cascade process is repeated until the total required pressure standoff is achieved. A T-S diagram (Figure 79) serves to further illustrate the thermodynamic cycle. Starting at the plenum pressure, P_o , the gas expands in the nozzle and window aperture reaching the final pressure at point b. The flow is shocked back in the duct, recompressed and cooled in the return loop and introduced to the next stage. The total power loss is

$$\Delta(P/m) = C_p \Delta T_o$$

The basic equations are presented in outline form as follows:

- a) The mass flow per stage (one dimensional nozzle flow)

$$\frac{m_i}{WH} = \frac{P_{oi} M_{a,i}}{(M_{a,i}^2 - 1)^{1/2}} \left(1 + \frac{\gamma-1}{2} M_{a,i}^2 \right)^{-\frac{\gamma+1}{(\gamma-1)}} \left(\frac{\gamma}{RT_{oi}} \right)^{1/2}$$

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Figure 78 Multiply Staged Expansion Aerowindow

H5194

Figure 79 T-S Diagram of the Flow Cycle of an Expansion Aerowindow

where

m = mass flow of the i th stage

WH = PAM output aperture

P_{oi} = inlet pressure at the supersonic nozzle

M_a = Mach number at the nozzle outlet

γ = ratio of specific heat

P_o = gas constant

T_{oi} = gas temperature at the nozzle inlet

b) The nozzle is sized so that the flow expansion angle spans the aperture (Prandl-Meyer angle)

$$\frac{h}{w} = (M_{a,k}^2 - 1)^{-1/2}$$

c) The power lost per stage is

$$\frac{P_{l,i}}{a_{oi} WH} = F_{i,a} \times G_{i,b} \times P_{a,i}$$

where

a_{oi} = sound speed of the inlet gas

$$F_{a,i} = \frac{\gamma M_{a,i} \left(1 + \frac{\gamma-1}{2} M_{a,i}^2\right)^{1/2}}{(\gamma-1) (M_{a,i}^2 - 1)^{1/2}}$$

$$G_{i,b} = \frac{2 \left(1 + \frac{\gamma-1}{2} M_{a,i}^2\right) \left[2\gamma M_{a,i}^2 - (\gamma-1)\right]^{1/2}}{M_{b,i}^2 (\gamma+1)^{(\gamma+1)/2}} - 1$$

M_b = flow Mach number after expansion

d) Summing the power lost per stage over all the stages, the total power lost in the A/W is

$$\dot{P}_L = WH \sum_{L=1}^N a_{oi} \frac{\dot{P}_L}{a_{oi} WH}$$

where

N = number of stages

The above set of equations represents the general operation of a series A/W. To formulate a specific design, the series stages are operated with the following conditions:

- a) $P_{b,i} = P_{a,i} + 1$
- b) $T_{oi} = \text{constant} = T_o$
- c) $M_{a,i} = M_a$
- d) $M_{b,i} = M_b$

In this manner, all the inlet temperatures are equal simplifying temperature control and all the Mach numbers of the stages are equal (for symmetry). This set of operating conditions may not represent a final optimization and a more detailed study would reveal if non-symmetric operation is useful. With these operating relationships, the following simplification is obtained

$$\dot{P}_L = WH a_o (\Delta P) \frac{FG}{1-J}$$

where

$$J = \left(\frac{1 + \frac{\gamma-1}{2} M_a^2}{1 + \frac{\gamma-1}{2} M_b^2} \right)^{\frac{\gamma}{\gamma-1}}$$

$$N = \frac{\ln P_\infty / P_L}{\ln J}$$

The next series of curves show these relations plotted as functions of the design Mach numbers. Figure 80 shows the power lost in the A/W normalized to the quantity.

$$W H a_o \Delta P$$

which represents the power lost through the PAM aperture if the aperture is treated as a sonic orifice. The power lost is plotted as a function of expanded Mach number for various inlet Mach numbers. It is clear that the power loss can be made arbitrarily small by approaching transonic flow. Decreasing the value of M produces large apertures and one approaches the design limits of transonic wind tunnels. In Figure 81, the number of stages is plotted vs the same expanded Mach number and as expected, operation at near transonic flow requires an enormous number of stages because the pressure drop per stage approaches unity. Cumulative losses in large numbers of stages will also limit the design. A cross plot of these two results is shown in Figure 82 where the minima of the power loss and number of stages is shown. A similar cross plot of power loss vs number of stages for various inlet Mach numbers is shown in Figure 83.

In this analysis, no account was taken of the losses encountered in the flow loop which ties the output of one stage to the input of another. The exact value of the loop losses depends critically on the design of the return loop and involve factors such as boundary layer control, efficient design of heat exchangers and turning vanes and a tradeoff between physical size of the return duct and cost. There is no standard loss factor and no little data base for such large diameter flow ducts. In the past, most large facilities were designed for quiescent flow rather than high efficiency and in other cases, economic factors are traded off against efficiency. In this study, the design of a large wind tunnel with a ~10% power loss has been used as an example to continue the design of the A/W. This choice is not meant to imply that more efficient tunnels cannot be designed but rather, the 10% loss figure represents present state of the art for wind tunnel design. At this point, the cost tradeoff between tunnel efficiency and power costs has been omitted.

The effect of loop losses is summarized by noting that the power loss is given by the product of volume flow rate and the total pressure lost around the flow loop, i.e.,

$$\dot{P}_{L,Loop,i} = \dot{Q}_i^o \Delta P_i^o = \eta_L \dot{Q}_i P_i$$

where η_L = loop efficiency = 10%. In terms of the previous notation

Figure 80 Power Consumed in the Aerowindow

Figuer 81 Number of Stages as a Function of Mach Number

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Figure 82 Minima of the Power Consumption and Number of Stages

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Figure 83 Power Loss vs Number of Stages in the Design of an Aerowindow

$$P_{L,Loop,i} = \eta_L \frac{\gamma-1}{\gamma} F_{i,a} P_{a,i}$$

the total losses are then the sum of the shock losses plus the loop losses and can be expressed as

$$\frac{P_{TOTAL}}{WH a_o} = FG \left(1 + \frac{\gamma-1}{\gamma} \frac{\eta_L}{G} \right) \sum_{L=1}^N P_i$$

where the first term in the bracket represents the shock losses and the second term, the loop losses. The power losses are thus modified by the factor

$$1 + \frac{\gamma-1}{\gamma} \frac{\eta_L}{G}$$

as a result of loop losses. This magnification is plotted vs Mach number in Figure 84. At high Mach number, the loop losses are insignificant when compared to the shock losses of the previous analysis. At low Mach numbers, where the shock losses are negligible, the viscous losses in the loop dominate and there is an optimum operating point for minimum total losses. This minimum is clearly illustrated in Figure 85 at a Mach number of ~1.2 for ~12 stages. At this minimum the power losses is approximately equal to the normalizing factor, $WH a_o \Delta P$.

The normalizing factor takes on a curious significance in that it represents the flow power which would be lost if no A/W were present and all the laser gas "fell" out the laser aperture. A well designed A/W optimized for minimum power loss must consume at least that much power. The advantage of the A/W, of course, is good optical quality through the window and no loss of laser gas (except for a small leakage). The cross plot of Figure 86 serves as a convenient design map.

The value of the normalizing parameter is

$$a_o \Delta P = 30 \text{ MW/m}^2$$

An estimate of the input electrical power per square meter of PAM aperture is

$$\sim 100 \text{ kJ/PAM} \times 10 \text{ Hz} \div 10\% (\text{efficiency}) = 10 \text{ MW/m}^2$$

H 5979

Figure 84 Effect of Return Flow Loop Losses in the Design of an Aerowindow

H5980

Figure 85 Optimum Design of a Closed Cycle Aerowindow

Figure 86 Design Map for a Closed Cycle Aerowindow

This estimate indicates that the aperture of an A/W must be less than the PAM aperture if high overall efficiency is to be maintained. There are several schemes including focusing, low pressure operation and beam multiplexing which can significantly lower the fraction of input energy which is required by the A/W. These issues will be discussed in following sections.

C. ALTERNATIVE AEROWINDOW CONCEPTS

The supersonic A/W when optimized for minimum power loss turns out to be only slightly supersonic and, hence, the possibility of operating a subsonic A/W was examined. The following analysis, presented in outline form, indicates the the power consumption for subsonic and supersonic A/W is comparable when both are optimized for minimum power loss.

In the subsonic A/W, the pressure gradient transverse to the flow velocity is offset by the centrifugal force of the curved flow. For subsonic flow the maximum pressure gradient is low so that several stages are needed to standoff the full pressure between the laser and the low pressure beam tube. Figure 87 shows a schematic of an N-stage subsonic A/W with window height W and radius of curvature R. The equation relating the window geometry to the flow properties is

$$\frac{W}{R} = \left[\frac{\frac{\gamma-1}{2} M^2}{1 + \frac{\gamma-1}{2} M^2 - (P_N/P_{NH}) \frac{\gamma-1}{\gamma}} \right]^{1/2} - 1$$

For simplicity it is assumed that all the flow Mach numbers for all the N-stages are equal and that all the stages sustain the same pressure ratio denoted by

$$f = P_\ell / P_u < 1$$

The total mass flow of all N-stages is given by

$$\dot{m}_{TOT} = \sum_{i=1}^N \rho_i V_i A_i = VA \sum_{i=1}^N \rho_i$$

since all the flow velocities and apertures of each stage are equal. It is further assumed that the temperature of each A/W stage is equal (heat exchangers are required in each loop) which implies that

J2302

Figure 87 Subsonic Multi-Stage Aerowindow

$$\sum_{i=1}^N p_i = \sum_{i=1}^N P_i / RT_i = (P_1 / RT) \sum_{i=1}^N (f_i)^{i-1}$$

or

$$\dot{m}_{TOT} = \frac{\gamma P_1 MA}{a} \frac{1-f^N}{1-f}$$

where

P_1 = laser cavity pressure

The number stages necessary to stand off the total pressure ratio P_1/P_{N+1}

$$N = \frac{\ln (P_{N+1})}{\ln f}$$

The power required to drive this flow, assuming that each stage of the A/W suffers the same compressor losses, is

$$P_L = \dot{m}_{TOT} RT \ln P$$

where P is the compressor pressure ratio. A typical value for the pressure across a compressor operating near Mach 1 is $\sim 20\%$, i.e., $P \cong 1.2$.

As a numerical example, consider a 1 atm laser and a 1 Torr beam tube with a pressure ratio per stage of 1.5, i.e., $f = 2/3$. The number of stages is given by

$$N = \ln 760 / \ln (2/3) \cong 16$$

Assuming a value of $R=W=1$ m, the Mach number associated with a pressure ratio of 1.5 is

$$M^2 = \frac{1 - f \frac{1-\gamma}{\gamma}}{\frac{\gamma-1}{2} \left(\frac{W}{R} + 1 \right)^{-2} - 1}$$

or

$$M = 0.905$$

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The mass flow is 1267 kg/s and the power flow is

$$P_1 \cong 15 \text{ MW/m}^2$$

This power loss corresponds to half that estimated for a supersonic A/W. However, in actual operation the stages of the subsonic A/W would be operated in series and additional loop losses would be generated. In this mode of operation each low pressure stage would have to be pumped up to the pressure of the next higher stage and, hence, extra pump power is required. An estimate of this extra pumping loss increases the power lost by ~30 to ~100% depending upon the efficiency of each pumping stage and total power estimates range from

$$P_L = 18-30 \text{ MW/m}^2$$

Another concept for an A/W is that of a subsonic stalled compressor which is shown schematically in Figure 88. A series of rotating blades induces a curvature in the flow as is well known from airfoil theory and the curvature of the flow balances the transverse pressure gradient in the same manner as a conventional subsonic A/W. For the four-bladed geometry shown in the figure, the output pulse would be timed to arrive during the clear aperture portion of the rotation cycle. Such a window could be used to multiplex several beam through simultaneously. The analogy of this configuration to a stalled compressor stems from the observation that only a small leakage flows through the compressor. In this sense, the analogy with a turbomolecular pump is appropriate.

Again, an outline of the relevant equations are as follows:

a) The lift per stage is given by

$$\text{Lift} = C_L \frac{\rho V^2}{2} A_B$$

where

C_L = coefficient of lift

A_B = area of the blade

H5387

Figure 88 Subsonic Axial Compressor as an Aerowindow

- b) The total pressure standoff is given by differential equation

$$\frac{dP}{dN} = C_L \frac{\rho V^2}{2} \frac{A_B}{A}$$

where

N = number of stages

A = area of the A/W

- c) This expression can be reduced to

$$\frac{dP}{dN} = C_L \frac{PV^2}{2RT} \beta = \frac{\beta C_L}{2} \gamma M^2 P$$

where

β = ratio of blade to window area

- d) Integrating to find the total pressure ratio

$$\frac{P_L}{P} = \exp \beta \frac{C_L}{2} \gamma M^2 N$$

- e) The power loss is associated with the drag on the blades

$$D = C_D \frac{\rho V^2}{2} A_B$$

where

C_D = drag coefficient

- f) The power loss per stage is

$$P_{L,STAGE} = DV$$

- g) The equation relating the power loss per stage is

$$\frac{dP}{dN} = \beta C_D A \frac{\rho V^3}{2} = \frac{\beta A C_D V^3}{2} \rho_L \exp \left(-\beta \frac{C_L}{2} M^2 N \right)$$

assuming constant temperature.

h) Integrating to find the power consumed,

$$P = \frac{C_D}{C_L} A V (P_L - P_\infty)$$

where the following relations have been used:

$$P_\infty = P_L \exp \left(-\beta \frac{C_L}{2} \gamma M^2 N \right)$$

$$P_L = \rho_L R T$$

$$\gamma R T = a^2$$

i) The velocity can be expressed as

$$V = \sqrt{\frac{2a^2}{C_L N} \ln P_L / P_\infty}$$

so that the total power consumed becomes

$$P_L = a A \frac{C_D}{C_L} (P_L - P_\infty) \sqrt{\frac{2}{\beta C_L \gamma N} \ln P / P_\infty}$$

For a lift to drag ratio of $C_L/C_D = 20$ which is representative of a high lift blade and blade to A/W area ratio of 20%, a pressure standoff of 1 atm to 1 Torr is achieved with 100 blades and a power loss of ~ 1 MW. Although this configuration appears very attractive in terms of consumed energy, the optical quality is questionable due to severe turbulence generated by the blades.

A final scheme for a low repetition rate A/W is the mechanical shutter concept shown schematically in Figure 89. Two mechanical shutters located at positions A and B which open to permit passage of the laser pulse. Louvred shutters are shown as a means of decreasing the opening and closing time instead of one large shutter, but other configurations such as rotating ball valves are possible as well. The operation of the windows proceeds as follows: Prior to arrival of the short pulse, the region between the shutters as well as the region to the right of the shutters is at low pressure. The shutters are opened to let the optical pulse pass through. During the open time, the

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Figure 89 Mechanical Shutter Window Concept

shock propagates down the tube followed by the contact surface which travels at the sound speed, a . The shutters are closed as rapidly as mechanical design will permit, ~ 10 ms. The spacing of the shutters is arranged so that all the escaped gas is trapped between them. Large vacuum pumps evacuate the space between the shutters in the interpulse time and the cycle repeats. Assuming isothermal and isovolume pumping conditions, the equation for the pressure in the region between the shutters is

$$\frac{dP}{dt} = -P \frac{S}{V_0}$$

where

S = pumping rate in liters/s

V_0 = volume of region between the shutters

Solving for this differential equation

$$S = \frac{V_0}{t_{INT}} \ln P_L/P_\infty$$

where

t_{INT} = interpulse time, $(PRF)^{-1}$

$V_0 = A a t_0$

A = area of the duct

a = sound speed

t_0 = open time of the shutters

A plot of the quantity SAt_{INT}/t_0 vs the pressure ratio P_L/P is shown in Figure 90. The energy assumed is given by

$$P_L = \dot{Q} \Delta P = A a (t_0/t_{INT}) \Delta P$$

As a numerical example, consider a 1 atm laser, 1 Torr beam transport tube, open time of 10 ms and PRF of 10 Hz. The volume of pumping speed required for a pressure ratio of $P_L/P_\infty = 760$ is $S = 1.8 \times 10^5$ l/s per square meter. The power consumed per square meter per atmosphere is plotted as a function pulse repetition frequency for various shutter opening

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Figure 90 Performance of Mechanical Shutter Window

times in Figure 91. For a shutter opening time of 10 ms and a repetition frequency of 10 Hz only a few megawatts per square meter at 1 atm is consumed. At low frequencies, this simple mechanical shutter may be the optimum window configuration.

D. SHEAR LAYERS AND OPTICAL QUALITY

All of the A/W concepts discussed thus far as well as any flowing gas curtains, all have velocity shear layers as an unavoidable feature. At many of these shear layers, a density or species gradient also may exist and the detailed structure of these features can play an important role in the optical quality of beams passing through them. The effect of velocity and species shear is to cause large turbulent mixing of material on either side of the layer. The turbulent mixing results in refractive index variations which scatter the laser beam causing severe distortions in the farfield intensity distribution of the beam.

One method of reducing the effects of these turbulent shear layers is to buffer the shear layers with an index matched flow as illustrated in Figure 92. In this illustrative example, it is desired to flow a gaseous saturable absorber between a nonflowing laser gas mixture and a nonflowing ambient air. A separate buffer flow of laser gas and nitrogen at the same flow velocity as the saturable absorber gas serves to separate shear layers from those across which the index of refraction jumps. On either side of the shear layer between the nonflowing and flowing laser gas, the index of refraction is unchanged and there is no optical degradation. There is a turbulent mixing layer between the flowing laser gas and the flowing saturable absorber but since both flows are matched in flow velocity there is no large-scale shear and the width and growth rate of the mixing layer is small.

Another example of the transition layer method of reducing turbulent mixing layers is shown in Figure 93. Here the buffer gas is used to reduce the mixing layer thickness at a temperature jump which can act in the same way to induce refractive index variations in the mixing layer.

The structure of velocity shear layers depends upon the Reynolds number. For low Reynolds numbers large, coherent density fluctuations, called "rollers," shown in Figure 94(12), can build up in the mixing layers. The optical degradations from such large-scale fluctuations add linearly as opposed to the square root dependence for small-scale fluctuations, and the optical quality of the beam is significantly degraded. However, these rollers persist only for low values of Reynolds number and high values of velocity shear. In the example, of Figure 94

Figure 91 Power Consumption of a Mechanical Shutter Window

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Figure 92 Index Matching Flow for Shear Layers

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Figure 93 Index Matching Flow for Temperature Discontinuities

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Figure 94 Coherent Density Fluctuations in Low Reynolds
Number Shear Layers

Figure 95 Growth of Turbulent Mixing Layers

Such parabolic behavior was clearly demonstrated at AERL(13) in the data shown in Figure 96. However, the data also shows that the use of high mesh screens greatly reduces the wake defect and inhibits the growth of the mixing layer. This measurement was performed on a large-scale device (35 cm) and interferometric studies indicate negligible degradation in beam quality. There are other techniques to reduce the wake defect such as gas injection in the wake, boundary layer suction, etc., but the screens are simple and over small areas at low flow speeds, are efficient.

For supersonic A/W's the use of screens are inappropriate and active techniques should be used. Again the gas used for the A/W can be identical to the static high pressure gas of the beam tubes so that there is no jump in index of refraction. In the supersonic regime, the mixing layers are thinner and the distance to reach the turbulence transition is short.

There are two effects which can reduce the optical quality of a beam passing through a supersonic aerowindow, one caused by steady variation and the other caused by non-steady disturbances. The steady effects are associated with the flow field structure of the A/W, i.e., the wave and shock systems through which the beam must pass. The unsteady effects are associated with turbulence in the shear layers. The steady effects can be removed by corrective optics in two ways as long as their spatial wave-length is not too small. For aerowindows of the pure compression or expansion type where there is only a single wave system in the aperture, there is only a linear variation of the optical phase front caused by wedge shaped regions of constant (but different) index of refraction. The total amount of beam steering can be calculated by summing the prismatic effect of each region and corrections for this steering term amounts to only a tilt of one optical surface. In hybrid types of aerowindows such as the shock expansion windows, shock waves and expansion waves systems interact and nonlinear variation in phase across the window aperture results. The phase variation is linked to the beam degradation by the relation:

$$\frac{I(\Delta\phi)}{I(0)} = \left[\frac{1}{A} \int_e i\Delta\phi dA \right]^2$$

where I is the peak for field intensity

A is the aperture

$$\Delta\phi \approx \frac{2\pi\beta L}{\lambda} \Delta\rho/\rho_s$$

β = Gladstone-Dale, coefficient

L = Optical path length

Figure 96 Growth Rate of Mixing Layer in the Absence of Shear

For $\Delta\phi \ll 1$

$$\frac{\Delta I(\Delta\phi)}{I(0)} = (\Delta\phi)^2$$

For a shock-expansion window, the phase variation has been calculated (14) in Figure 97, and the far field intensity degradation in Figure 98. For a 10 cm window, at a shockwave pressure ratio of 1.2, the loss in far field intensity is less than 1%. The calculation has not been repeated for 1 m size windows, but it is not probably worse than 10% and is correctable by "rubber mirror" techniques.

The second and potentially more damaging cause of optical degradation is that caused by turbulence in the mixing layers. The reduction in far field intensity caused by turbulent fluctuations in the refractive index is given by

$$\frac{I(\Delta\eta)}{I(0)} = e^{-2(\Delta\eta)^2 k^2 \Lambda L}$$

where $\Delta\eta$ = the fluctuation in refractive index

k = the wave number of the laser

Λ = the integral scale of the turbulence

L = optical path length through the turbulent shear layers

Again when the exponent is small, the change in far field intensity is given by:

$$\frac{\Delta I(\Delta\eta)}{I(0)} = 2(\Delta\eta)^2 k^2 \Lambda L$$

- The above expression for beam degradation due to fluctuations can be put in a form similar to that for degradation by (steady) phase variations by noting that

$$(\Delta\eta) \approx 20\% \beta \frac{\Delta\rho}{\rho_s}$$

for many turbulent situations. The turbulent far field degradation can then be expressed as

$$\frac{\Delta I(\Delta\rho/\rho_s)}{I(0)} \approx \left(0.2\sqrt{5} k \beta L \frac{\Delta\rho}{\rho_s} \sqrt{\frac{\Lambda}{L}} \right)^2 : \text{turbulent}$$

Figure 97 Phase Distortion in a Shock-Expansion Aerowindow

Figure 98 On Axis Intensity Degradation in a Shock-Expansion Aerowindow

as compared to

$$\frac{\Delta I (\Delta \rho / \rho_s)}{I(o)} \approx \left(k \beta L \frac{\Delta \rho}{\rho_s} \right)^2 \quad \text{steady}$$

Aside from the numerical factor, the turbulent expression differs from steady form by the factor $\sqrt{\Lambda/L}$ which can be interpreted as a random walk factor among turbules of size Λ uniformly distributed along a path length, L .

A plot of $\Delta I (\Delta \rho / \rho_s) / I(o)$ versus $\Delta \rho / \rho_s$ for turbulent fluctuations is shown in Figure 99. For small size windows, a $\Delta \rho / \rho_s$ near unity can be tolerated with very little far field beam degradation. Experiments with a 4 cm aperture⁽¹⁴⁾ window show no discernible decrease in far field intensity (see Figure 100). For meter size aerowindows, the value of $\Delta \rho / \rho_s \leq 0.1$ to keep optical degradation below the 1% level. In this calculation the growth rate of the mixing layer is approximated by a linear growth with thickness of 10% of the distance along the layer. This estimate of growth rate is applicable to shear layers with velocity shears of $\sim 50\%$ and is, therefore, a conservative as will be shown by a calculation of the momentum deficit of $\sim 25\%$. Also used in this calculation is a typical turbulent scale size of 10% of the mixing layer thickness.

A detailed calculation of $\Delta \rho / \rho_s$ for an energy efficient, low Mach number, staged aerowindow indicates that beam degradation by turbulent fluctuations are negligible. A schematic diagram of the aerodynamic processes which occur at each stage of such an A/W is shown in Figure 101. At the edge of each splitter plate which separates the flow between stages, the flow is deflected towards the lower pressure of the adjacent stage. As a result of this flow deflection, a weak, oblique shock and expansion fan are formed at the edge of each splitter plate. To minimize optical deflection, the densities, and hence, the temperatures equal are made equal. On the shock side of the splitter plate, the flow is decelerated while on the expansion fan side, the flow is accelerated, hence there is a velocity shear across the layer. The magnitude of the shear is determined using weak shock and expansion expressions and is found to be

$$\Delta u \approx 0.25 u_{\max}$$

In this design $u_{\max}$ was chosen to be $M = 1.3$. The Reynolds number for which transition to turbulence occurs is of the order of 7×10^4 so that the flow distance along the shear layer at which the shear layer goes turbulent is

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Figure 99 Strehl Intensity vs Density Fluctuations

Figure 100 Far Field Intensity Through a 4 cm Aerowindow

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Figure 101 Schematic Diagram of Turbulent Processes in the Shear Layers of a Multi-Stage Aerowindow

$$x_{TR} \cong 7 \times 10^4 \times \frac{\nu}{\Delta u}$$

where ν is the kinematic viscosity = η/ρ

η is the viscosity $\cong 200$ μ Poise

ρ is the gas density $\cong 10^{-3}$ g/cm³

The transition distance for these values is of the order of a centimeter and, hence, the shear layers in the supersonic aerowindows are turbulence within a very small fraction of the total aperture.

After the transition, there is a development regime until self-preserving turbulence is obtained. This self-similar regime occurs at Reynolds numbers of the order of 4×10^5 which requires a flow distance of about 10 cm. At this point, the phase error caused by density fluctuations can be estimated. The calculation involves a detailed description of temperature and density as gas on either side of the mixing layer flows through the shock and expansion fan. The flow passing through the shock is decelerated while the flow through the expansion fan is accelerated. This shear in velocity creates a wide turbulent mixing layer. The pressure on either side of the mixing layer is equal so that only temperature differences can give rise to density gradients. These temperature differences are calculated using the approximate solutions for shocks and expansion fans. The results are

$$\frac{\Delta T}{T_L} = \frac{\Delta \rho}{\rho_L} \cong 5 \times 10^{-3}$$

With reference to Figure 99, this level of density fluctuation is far below that which will degrade the on-axis intensity of a beam by 10% for a 1 m A/W, at least for a single stage A/W.

To estimate the optical losses for an N-stage A/W two conservative assumptions regarding the correlative nature of the phase variations of successive stages are made. The first assumption is that the turbulence is 100% of average value of $\Delta\rho/\rho$ rather than the 10% estimate for fully developed turbulence. Second, it is assumed that all the turbulence is correlated so that the optical phase errors add linearly rather than as $\sqrt{N}$ which would be the case for uncorrelated, random walk, turbulence. With these assumptions, the optical phase error per mixing layer is

$$\Delta\phi_i = \frac{2\pi\beta L}{\lambda} \frac{\Delta\rho_i}{\rho_L} = \frac{2\pi\beta L}{\lambda} \frac{\Delta\rho_i}{\rho_i} \frac{\rho_i}{\rho_L}$$

where β is the Gladstone-Dale coefficient

λ is the laser wavelength

L is the thickness of the mixing layer

For a constant value of $\Delta\rho_i/\rho_i$ the total optical phase error is

$$\Delta\phi = \sum_{i=1}^N \Delta\phi_i = \frac{2\pi\beta L}{\lambda} \frac{\Delta\rho}{\rho} \sum_{i=1}^N \frac{\rho_i}{\rho_L}$$

For an N stage aerowindow

$$\frac{\rho_i}{\rho_0} = \frac{\rho_\infty}{\rho_L}^{(i-1)/N}$$

so that

$$\begin{aligned} \Delta\phi &= \frac{2\pi\beta L}{\lambda} \frac{\Delta\rho}{\rho} \sum_{i=1}^N \frac{\rho_\infty}{\rho_L}^{(i-1)/N} \\ &= \frac{2\pi\beta L}{\lambda} \frac{\Delta\rho}{\rho} \frac{1 - \rho_\infty/\rho_L}{1 - (\rho_\infty/\rho_L)^{1/N}} \end{aligned}$$

To estimate the numerical value of the optical phase shift, a linear growth rate of 5% is assumed for the 25% momentum deficit (see Figure 95). The growth rates of supersonic shear layers is less, but the subsonic conservative estimate is retained.

For the specific optimized design parameters,

$N = 12$ stages

$P_L = 760$ Torr

$P_\infty \approx 1$ Torr

$M_{AW} = 1.3$

$\Delta\rho/\rho = 5 \times 10^{-3}$

$\beta \approx 2 \times 10^{-4}$ (nitrogen)

The total optical phase error is

$$\Delta\phi = 8 \times 10^{-2}$$

which yields an axis intensity loss of

$$\Delta I/I_0 = (\Delta\phi)^2 = 0.6\%$$

Thus even for the severe restrictions of 100% fluctuations and fully correlated turbulence, the optical degradation is minimal.

There is one other possibility of optical degradation in this type of multistage A/W, that being the shot-to-shot repeatability as influenced by possible instabilities in the position of the shocks or mixing layers. If the mixing layers wander, the beam position will not be changed significantly because the density difference on either side of the layer is only $\Delta\rho/\rho = 5 \times 10^{-3}$. If the position of the shock wanders from shot to shot, that could cause a beam tracking jitter. The effect would be to introduce an additional wedge of gas whose thickness varied. The phase difference at the widest part of the wedge is

$$\begin{aligned}\Delta\phi &= \frac{2\pi\beta L}{\lambda} \Delta\rho/\rho \\ &\cong \frac{2\pi \cdot 2 \times 10^{-4} L}{10 \cdot 6 \times 10^{-4}} \times \Delta\rho/\rho \\ &\cong 0.4\pi L\Delta\rho/\rho\end{aligned}$$

For the 12-stage aerowindow design

$$\begin{aligned}\Delta\rho/\rho &\cong \frac{\rho_i - \rho_{i-1}}{\rho_i} \\ &= 1 - \frac{\rho_{i-1}}{\rho_i} \\ &= 1 - [1 \text{ Torr}/760 \text{ Torr}]^{1/2} \\ &\cong 0.4\end{aligned}$$

Thus for a shock wander of L cm, the phase shift is

$$\Delta\phi \cong 0.16\pi L$$

The possible tilt in the phase front due to this wedge is compared with the diffraction angle,

$$\theta_D \cong 2 \frac{\lambda}{X} \cong 20 \text{ } \mu\text{rad}$$

for a 100 cm beam. The wavefront tilt equated to the diffraction angle is expressed as

$$\frac{\Delta\phi}{2\pi/\lambda} / 100 \text{ cm} \cong 20 \text{ } \mu\text{rad}$$

or

$$\frac{0.16\pi L\lambda}{2\pi \times 100} = 20 \text{ } \mu\text{rad}$$

or

$$L \cong 25 \text{ cm}$$

It is highly unlikely that the shock wander could be this large and it is concluded that such effects are negligible.

E. AIR BREAKDOWN IN AEROWINDOWS

The optical qualities of a well designed supersonic A/W appear excellent but estimates of the power consumption involved for these large aperture systems indicates that either aperture must be reduced and/or methods of multiplexing several beams through one A/W are needed. The minimum size aperture for an A/W relies, in part, on the breakdown characteristics of gases used for the A/W flow, i.e., clear air, dry N₂, etc. It is found that these typical gases have similar breakdown characteristics as shown for air in Figure 102. Also, the pressure dependence of the breakdown flux is weak in the range 1/2 - 2 atm and as a first order estimate, the curve of Figure 102 will be used throughout this pressure range.

The first implication is that low pressure operation of the PAM, i.e., ~ 1/2 atm is desirable since the breakdown characteristics and, hence, minimum aperture size are not sensitive

Figure 102 Clean Air Breakdown Flux for CO₂ Lasers

to pressure (near 1 atm) and the power consumption is directly proportional to pressure. The second implication of the curve of Figure 102 is that, since the breakdown flux (W/cm^2) falls slower than the breakdown time, a higher fluence (J/cm^2) can be passed through the same aperture at longer pulsewidths. For example, the maximum fluence at 1 ns is $\sim 20 \text{ J}/\text{cm}^2$ but $\sim 100 \text{ J}/\text{cm}^2$ can be passed at 10 ns. In the long time limit the clean air / breakdown flux levels off at close to $\sim 3 \times 10^9 \text{ W}/\text{cm}^2$ and enormous fluences can be handled in microsecond pulses. It is, therefore, advantageous to pass several short pulses through in sequence fashion to create the illusion of long pulses since the peak flux is not strongly reduced.

There is a difficulty with the practice of reducing the beam diameter to the fluence level of air breakdown in the following sense. Taking, as a long time limit, the maximum flux as the value at 100 ns which is $8 \times 10^9 \text{ W}/\text{cm}^2$, the fluence in a single 5 ns pulse could be as high as $40 \text{ J}/\text{cm}^2$ which is at least a factor of 4 in excess of the mirror damage limit. Therefore, to achieve a $40 \text{ J}/\text{cm}^2$ fluence in the A/W aperture, the beam must be focussed to that fluence and recollimated on the other side of the A/W as illustrated in Figure 103(a). However, an estimate of the focal length required to reduce the beam area by a factor of 4 from $10 \text{ J}/\text{cm}^2$ (mirror limit) to the $40 \text{ J}/\text{cm}^2$ air breakdown is

$$f \cong \frac{D^2}{4\lambda}$$

which is 25 km for a one-meter diameter beam and clearly too long. The other alternative shown in Figure 103(b) is an internal focal point with the A/W placed at the $40 \text{ J}/\text{cm}^2$ on the way to a hard focus. Here the mirror separation is dependent upon A/W length and can be made quite reasonable. However, an internal focal point requires a hard vacuum on the low pressure side of the A/W rather than the ~ 1 Torr presently envisioned. (The ~ 1 Torr estimate stems from soft x-ray shielding arguments which will be discussed in the following sections.)

There are two possible configurations which follow from the requirement of a hard vacuum on the low pressure side of the main A/W. The first would place the internal focal point at the pellet in which case, the entire A/W is placed between the last optic and the pellet. Although this scenario is not impossible it is difficult to imagine a configuration with this concept which does not have something of the order of one A/W per beam line. The high-power consumption of an A/W probably rules out this concept.

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Figure 103 Focusing Concepts Through an Aerowindow

The second configuration would have the main A/W stand off the PAM cavity pressure from the hard vacuum and the x-ray shielding A/W be double sided, i.e., a ~ 1 Torr pressure in the middle of two A/W standing off low pressure on either side. Since the pressure for which breakdown effects are unimportant may be as low as 0.01 Torr, the number of stages and complexity of the main as well as the shielding A/W increases significantly and this entire approach is not recommended.

A simple approach to multiplexing several beams through a single A/W is shown in Figures 104 and 105. The output of a PAM is

$$\frac{2}{3} N_{\text{eff}} g_o L_c E_{\text{so}} P \text{ (J/cm}^2\text{)}$$

where

N_{eff} = increase in first pulse energy as a result of multiplex extraction,

$g_o L_c$ = gain-length product

E_{so} = saturation fluence at 1 atm

P = cavity pressure in atm

Typically, the output fluence is 5 J/cm² at 1 atm. In order to make the system more compact, it is advisable to reduce the beam area as soon as possible to the 10 J/cm² fluence limit of the mirrors. A parallel grouping is performed simultaneously on as many beams (at 10 J/cm²) as required to raise the fluence to the air breakdown limit in the A/W. The parallel grouping is done by overlapping beam spots in the A/W until the desired fluence is reached. All the overlapped beams are collimated and arrive at the same time.

A second grouping of parallel beams, is done in series at intervals of the short-pulse duration. This sequencing of beam bundles is repeated until as many beams pass through the A/W as desired. As far as air breakdown is concerned, the maximum flux is that given by Figure 102 based upon a total time equal to the product of the short-pulse duration and the number of seriesed beam bundles.

As an example, the 100 ns time limit on breakdown flux of $\sim 8 \times 10^9$ W/cm² is assumed. For a 5 ns pulse, the maximum fluence is then 40 J/cm² or 4 times the fluence limit for the mirrors. Thus 4 PAM outputs are combined into one beam bundle. Twenty of these beam bundles could be sequenced through the A/W

J1510

Figure 104 Beamline Grouping to Increase Fluence for Aerowindow Multiplexing

Figure 105 Beamline Staging for Aerowindow Multiplexing

to make up the 100 ns total pulse duration for a total multiplex count of 80 PAMS. On the basis of just air breakdown, all the PAMS from a 1-5 MJ Laser Fusion Driver could be passed through one A/W thereby decreasing the fraction of total input power required to run the A/W. In this example, a one square meter A/W consuming 30 MW at 1 atm requires only 10% of the input energy for a 3 MJ output at 10 Hz and 10% intrinsic laser efficiency.

The primary limitation on the number of beams which can be passed through the A/W stems from the complexity of the optical configuration. A mirror system must combine the beams in the series-parallel arrangement and then separate these beams temporally as well as spatially to insure that the target is irradiated with the required spatial distribution and within the required temporal jitter.

Another aspect of this design concept is that the aperture of the A/W is equal by the PAM aperture reduced by the ratio of PAM output fluence to mirror fluence, i.e.,

$$\text{A/W aperture} = \text{HW} \frac{\frac{2}{3} N_{\text{eff}} g_{\text{O}} L_{\text{C}} E_{\text{SO}} P}{\text{MIRROR FLUENCE LIMIT}}$$

With this scaling limit, it may not be advantageous to make the PAM aperture as large as possible since the A/W and hence the A/W power will be large. In terms of the total number of PAMS, the ratio of energy consumed in the A/W to total input power is

$$\frac{P_{\text{AW}}}{P_{\text{EL}} \cdot \text{INPUT}} = \frac{30 \text{ MW/M}^2}{N_{\text{pam}} \times \text{MIRROR FLUENCE} \times \text{PRF}/\eta}$$

where

η = total system efficiency

N_{pam} = number of PAMS

A map of this power ratio vs PRF and number of PAM is given in Figure 106.

An alternative to the series-parallel beam multiplexing which allows the A/W aperture to be reduced involves segmenting the PAM output beam. Using a segmented mirror, the output beam is split into given number of beams, which is directed separately through the A/W aperture in an overlap manner. The beams are recombined in the low pressure region after the A/W. The optical

J2324

Figure 106 Ratio of Aerowindow Power to Input Power

arrangement is similar to the series-parallel configuration in that once the breakdown flux is reached by overlapping additional beams bundles are sequenced through at intervals. The difference between the two methods is that unless a corrective optics system is used full PAM aperture will be lost and the spot size on the pellet is increased. It is not clear at this stage whether the phase coherent optical system need be adaptive (i.e., correctable on a pulse-to-pulse basis) or just corrective (i.e., adjusted once and set in a semi-permanent manner.) The former type optical systems tend to be expensive requiring many actuators to change the surface figure of the mirror. The latter type may be adjusted as part of the fabrication process on an optical test bench and are significantly less expensive.

F. PERFORMANCE AND CHARACTERISTICS OF SALT WINDOWS

Materials for CO₂ laser windows generally fall into two categories, alkali halide crystals such as NaCl and KCl and covalent type materials such as Ge, CdTe, GaAs and ZnSe. Among the alkali halides KCl has the lowest reported absorbtivity, as low as $\sim 10^{-4}\text{cm}^{-1}$ in the best samples with NaCl higher at $\sim 10^{-3}\text{cm}^{-1}$. In the covalent materials, ZnSe has an absorbtivity of $\sim 10^{-3}\text{cm}^{-1}$. The single-pulse-damage threshold for nanosecond pulses is highest for NaCl at $\sim 5 \text{ J/cm}^2$. For Laser Fusion Driver applications where the single-pulse-damage threshold is the dominant performance characteristic, NaCl is the preferred. The average power limit is exceeded only at the maxima of the fluence, pulse repetition rate and window diameter. The thickness to diameter ratio for a round window depends slightly on the method of clamping. With a standard safety factor of 4 the thickness to diameter ratio is approximately

$$\frac{t}{D} \approx \sqrt{\frac{P}{Y}}$$

where

t = window thickness

D = window diameter

P = pressure difference across the window

Y = yield stress

For NaCl, the yield stress varies between

$Y = 0.350 \times 10^3 \text{ psi}$ - single crystal

$\approx 1.4 \times 10^3 \text{ psi}$ - polycrystalline

-215-

The large diameter salt windows recently developed for DOE by Harshaw Chemical Co. are polycrystalline and hence the t/D ratio for 1 atm pressure standoff is

$$t/D \cong 0.1$$

thus a 50 cm diameter window is 5 cm thick and will develop a temperature differential across itself of

$$\Delta T \cong \frac{1}{2} \beta P t^2 / K$$

where

β = absorptivity, cm^{-1}

P = incident power density, W/cm^2

K = heat conductivity, $\text{W}/(\text{c}, ^\circ\text{K})$

where the assumption has been made that the window is face cooled. (It will be shown that edge cooling only serves to enhance transverse temperature gradients which introduce distortions in the output beam.) For the worst conditions of

$$\beta = 10^{-3} \text{ cm}^{-1}$$

$$P = (5 \text{ J}/\text{cm}^2) (10 \text{ Hz}) = 50 \text{ W}/\text{cm}^2$$

$$t = 5 \text{ cm for } D = 50 \text{ cm}$$

$$K = 80 \text{ mW}/\text{cm}^\circ\text{K}$$

the temperature rise is $\Delta T \cong 80^\circ\text{K}$. The critical temperature for which thermal fracture will occur is

$$\Delta T_{\text{CRIT}} \cong \frac{2y}{\alpha E}$$

where

y = yield stress

α = coefficient of linear expansion $\cong 24 \times 10^6 / ^\circ\text{F}$

E = Young's modules $\cong 5.6 \times 10^6 \text{ psi}$

For NaCl, the critical temperature is $\Delta T_{\text{CRIT}} = 12^\circ\text{K}$, and hence only at the highest fluences and PRF is the thermal fracture limit exceeded. The primary considerations for choice of the material is the maximum operating fluence level consistent with long lifetime.

Many damage and lifetime studies on NaCl windows have been reported in the literature but few measurements in the nanosecond regime and with beams of sufficient quality to rule out effects associated with "hot spots" and other anomalies. In addition, there is confusion as to what constitutes window damage and what is the definition of the useful lifetime of the salt window. A large portion of this confusion has been cleared up in a series of experiments performed at Los Alamos (15) in which the useful lifetime of salt windows was defined in terms of the focussability of nanosecond CO_2 laser beams which passed through the window. When the definition of window lifetime vs incident fluence showed a well defined lifetime. At a given fluence level, the optical quality of the transmitted beam showed a sharp drop over a narrow range of shots indicating window damage. The observation (16) was made that by repolishing the window the optical quality returned so it is suggested that the primary damage mechanism is surface crazing. Although the attempt was made to stabilize the total energy fluence and spatial distribution in the test, there was an experimental uncertainty in the incident energy density and it is possible that air breakdown occurred ahead of the window causing even more uncertainty with regard to surface damage mechanisms. A summary of the data is presented in Figure 107.

The limited quantity of the data points out a glaring gap in the data base for this important window option. Data must be extrapolated by six orders of magnitude to reach the practical lifetime limit of one year at 10 Hz. Also, the uncertainty caused by the fluence error bars leaves the estimate at a scaling low as pure guess work. The load line indicating a lifetime dependence with the -6.9 power of fluence would give a working fluence of $\sim 0.3 \text{ J/cm}^2$ for a one-year life. Such a power law is not unusual in the lifetime of optical glasses. For example, the lifetime of quartz jacketed, flashlamps also obeys a steep power law (~ -8 to -9) of the input energy.

An estimate provided by Los Alamos of 2 J/cm^2 with long life is shown on the figure. This estimate results from the belief that damage is caused by plasma formation out ahead of the salt window and if the fluence is kept below the plasma formation limit, then no degradation in the optical quality of the beam is predicted.

A quick estimate of the price of the salt windows pinpoints the economic problems involved. Present costing for 18-in. dia. salt windows is \$2.5 million for 100 salt windows 3.5-in. thick. These windows are used at a cavity pressure 1800 Torr so that the t/D ratio is

J1504

Figure 107 Salt Window Lifetime Data

$$t/D \cong \sqrt{P/Y} \cong 0.22$$

For an 18-in. dia. window, the required thickness is ~ 4 in. The present cost per square centimeter is \$15. The present yield is $\sim 24\%$ and the projected yield should increase to 60% with an additional 30% cost savings according to Harshaw Chemical Co. estimates. The projected diameter could increase to 60 cm. The required thickness would remain essentially the same since the diameter increase is offset by a decrease in PAM cavity pressure, i.e., ~ 1 atm.

Assuming the $1/3 \text{ J/cm}^2$ for one-year operation is correct, the \$15/cm² price translates into a capital cost of \$45 million for the three million square centimeters involved for an energy on target of 1 mJ and a PRF of 10 Hz. Clearly, the salt windows must be refurbished at a small fraction of their capital cost or else these recurring costs will be impractical.

The question of average power handling capability was discussed briefly in terms of the upper limits of incident average power. In this section the effects of the techniques of cooling the salt window on the optical beam quality will be discussed. There are two methods of removing heat from the salt window, edge and face cooling. For these dielectric windows which have such poor thermal heat conductivities, it is always preferable to extract heat through the shortest dimensions to avoid large temperature gradients. The thickness to diameter ratio is in the range 0.1 to 0.2, and hence face cooling is the logical choice. Edge cooling will give beam distortions in the window even for uniformly illuminated windows. The primary effect for edge cooling is a focus defect with focal length

$$f = \frac{D^2}{8At\Delta T}$$

where A is a constant which for salt is $\cong 10^{-5}^\circ\text{K}$. However, the temperature difference across the radius of a purely edge coded salt window is

$$\Delta T \cong \frac{\beta R_s^2}{K} P$$

where

R_s = radius of salt window

β = absorbtivity

P = incident power chemistry

K = heat conductivity

For an incident power of $1/3 \text{ J/cm}^2$ at 30 Hz, the $\Delta T \cong 40 \text{ }^\circ\text{K}$ for a 20 cm diameter window which is above the thermal fracture limit. Hence, edge cooling is impractical for Laser Fusion Drivers.

A numerical integration of the heat conduction equations has been carried out to determine the heating profile for a given spatial profile of incident laser power. For perfect face cooling, i.e., no variation in coolant temperature across the face, and insulated edges, a spatially uniform beam will give a heating profile which is uniform in the radial dimension. There will be a thermal gradient in the propagation dimension because the vacuum side of the window is uncooled but gradients in this direction do not affect the optical quality of the beam. The illumination geometry which was modeled is shown in Figure 108 and the other parameters are:

$$\text{incident power} = 100 \text{ W/cm}^2 \times \text{window area}$$

$$\text{thickness to diameter ratio} = 0.2$$

$$\text{spatial profile of incident power, } I/I_0 = \exp [-c(R/R_s)^2]$$

Three profiles are plotted, a "top hat" where $c=0$ and a first and second order Gaussian, i.e., $c=1, 2$ respectively. For the Gaussian profiles, the total integrated power was kept equal to the top hat case. The spatial profile of temperature for these intensity distributions is shown in Figure 109 for three planes through the salt window thickness, the laser incident, middle and vacuum sides. The heat conduction in the radial dimension is so poor that the spatial profile of the temperature reproduces the incident power profile.

The effect of this spatial profile on beam quality is estimated by using the temperature dependent refractive index for salt

$$\frac{dn}{dT} \cong -2 \times 10^{-5} / ^\circ\text{C at } \lambda = 10.6 \text{ } \mu$$

The phase distortion is calculated by averaging the temperature distribution across the window at each radius and using the above refractive index profile. Elongations and deflections as a result of the unequal thermal distribution are also calculated and their effect on the optical path length is computed. The phase distortion for the three-beam profiles is shown in Figure 110. Such large phase distortions, if left uncorrected, will reduce the on-axis intensity of the far field beam drastically, i.e.,

$$\frac{I(\Delta\phi)}{I(\Delta\phi=0)} \cong e^{-(\Delta\phi)^2}$$

-220-

J1516

Figure 108 Illumination Geometry for Face Cooled Salt Windows

Figure 109 Spatial Profile of Temperature Distributions in a Salt Window.

J1520

Figure 110 Phase Distortions in Face Cooled Salt Windows

A phase distortion of $\Delta\phi = 2$ radians corresponding to the first order Gaussian profile, reduces the on-axis intensity to about 2% of its original value.

The phase distortions described are steady and predictable providing the beam profile is likewise. Non-steady variations will require an adaptive optics system to sense window induced phase distortions and correct them on a pulse to pulse basis. Steady variations are easily removed by a fixed corrective plate.

G. METHODS OF BEAM MULTIPLEXING THROUGH SALT WINDOWS

The obvious advantage of a salt window is that the pressure interface can be made directly at the PAM cavity leaving the beam multiplex and transport tubes at low pressure. Also, the PAM cavity could be operated at higher pressures than with the A/W option thus increasing the energy density, decreasing the size and increasing the extraction efficiency on a single rotational line. There are two basic configurations for the placement of the S/W. Either the S/W directly caps the PAM cavity with all other multiplex optics located external to the cavity or the windows are located at the end of the multiplex train in much the same place as the A/W. In the latter case, all multiplex optics are located on the high pressure side of the S/W.

The former case is the more direct approach and is examined first. There are two multiplex schemes which can be used with S/W mounted directly on the PAM cavity. These two options are outlined schematically in Figures 111 and 112. The N-pulse - 1 pass concept has N pulses each delayed with respect to each other by a vibrational repump time, $\sim 2 t_{vv} \approx 200$ ns at 1 atm. These pulses are derived either from N separate oscillators or from one large pulse split into N-beams and suitably delayed. The N-beams each pass through the PAM and are recombined to remove the time delays so that the beams all impinge on the target simultaneously. The second multiplex scheme uses a 1 pulse and reflects that pulse back through the PAM with a time delay of $2 t_{vv}$ to permit vibrational repump.

For both these multiplex schemes, pulses must pass through the salt windows with time delays of $\sim 2 t_{vv}$ and since it is advantageous to design the windows to operate at their fluence limits to reduce the number of windows, the question of repetitive operation of S/W (on a hundred nanosecond time scale) is critical. This is another instance where knowledge of the damage mechanisms for S/W is lacking and much needed.

Experimental data⁽¹⁷⁾ shows that damage thresholds in excess of 10^3 MW/cm² for 10^{-7} s pulses are possible. The damage fluence of $\sim 10^2$ J/cm² for 10^{-7} s is increasing faster than the $\sqrt{t}$ dependence when compared with the 5 J/cm² damage fluence for

Figure 111 N-Pulse, 1 Pass Multiplex Concept

Figure 112 1-Pulse, N Pass Multiplex Concept

nanosecond pulses. There is clearly a peak power mechanism present and perhaps air breakdown is limiting the short pulse working fluence. At 5 J/cm^2 in 10^{-9} s pulses, the resultant peak power of $\sim 5 \times 10^9$ is close to the air breakdown limit.

The mechanism of short-pulse breakdown is not resolved, but it appears safe to assume that several pulses, each at the nanosecond pulse fluence limit can be passed through the S/W at interpulse times of $\sim 100 \text{ ns}$.

For the 1 pulse -N pass multiplex case where 1 pulse passes through the PAM cavity ~ 5 times until it drains most of the energy stored in the nitrogen vibrational manifold, the fluence of the final pass is constrained by the S/W,

$$N_{\text{eff}} E_{\text{so}} P (1 + 2/3 g_o L_c) \leq \phi_s$$

where ϕ_s = salt working fluence limit, which is 1 J/cm^2 at present and 2 J/cm^2 projected. The unity within the bracket arises from the assumption that the PAM is operated under saturated conditions and that the input fluence is equal to the saturation fluence. The S/W fluence constraint sets an upper limit on the PAM pressure beyond which the $g_o L_c$ product must decrease. For example, if $g_o L_c \leq 10$ by the virtue of the parasitic limit, then

$$P_{\text{MAX}} \leq \frac{\phi_s}{N_{\text{eff}} E_{\text{so}} [1 + \frac{2}{3} (g_o L_c)_{\text{MAX}}]}$$

For a saturation fluence of $E_{\text{so}} = 1/3 \text{ J/(cm}^2\text{-atm)}$, $(g_o L_c)_{\text{MAX}} = 10$, $N_{\text{eff}} = 2.5$ and $\phi_s = 1$ and 2 J/cm^2 , the maximum pressure is 0.16 atm and 0.32 atm , respectively.

The number of PAM is given by

$$N(\text{PAM}) = \begin{cases} \frac{2E_T}{N_{\text{eff}} E_{\text{so}} P (1 + \frac{2}{3} g_o L_c) D^2} ; P < P_{\text{max}} \\ \frac{2E_T}{\phi_s D^2} ; P > P_{\text{max}} \end{cases}$$

when D is the diagonal of the PAM cross section (assumed square). However, each PAM requires an input pulse at the saturation fluence so that the total energy which must be supplied by preamplifiers is

$$N(\text{PAMs}) \frac{D^2}{2} E_{so} P$$

Assuming that the preamplifiers are identical in design to the PAMs the total number of PAMs required, the power amplifiers plus the preamplifiers is

$$\sum N(\text{PAMs}) = \begin{cases} \frac{2E_T}{N_{eff} E_{so} P (1 + \frac{2}{3} g_O L_C) D^2} \left[1 + \frac{1}{N_{eff} (1 + \frac{2}{3} g_O L_C)} \right]; & P < P_{MAX} \\ \frac{2E_T}{\phi_S D^2} \left(1 + \frac{E_{so} P}{\phi_S} \right); & P > P_{MAX} \end{cases}$$

This model is valid as long as the input fluence to the PAM is less than the S/W fluence which occurs at a pressure of

$$P = \phi_S / E_{so} \begin{cases} 3 \text{ atm}; \phi_S = 1 \text{ J/cm}^2 \\ 6 \text{ atm}; \phi_S = 2 \text{ J/cm}^2 \end{cases}$$

The total number of PAMs for the 1 pulse -N pass case with S/W capping the ends of the PAMs is plotted in Figure 113 for two values of S/W diameter and two fluence levels.

The large number of PAMs required per megajoule of laser energy on target indicates that this form of multiplexing is inappropriate for the configuration where the S/W cap the PAM ends. It is also interesting to note that the optimum pressure is subatmospheric and, hence, the strength properties of material windows are not being fully utilized.

The alternate multiplex configuration, i.e., the N-pulse - 1 pass shown in Figure 111 is preferred. In this scheme the maximum pressure is set by the first pulse energy which is usually the largest; (pulse extraction early in the pumping cycle could be arranged so that all pulses are approximately equal).

J2354

Figure 113 Number of PAMS for the 1 Pulse-N Pass Case with Salt Windows Capping Each PAM

Figure 114 Number of PAMS for the N Pulse-1 Pass Case with Salt Windows Capping Each PAM

$$P_{MAX} \leq \frac{\phi_s}{E_{SO} \left[1 + \frac{2}{3} (g_O L_C)_{MAX} \right]}$$

which is 0.4 atm and 0.8 atm for $\phi_s = 1$ and 2 J/cm², respectively. The number of PAMs is given by the same expression as the 1 pulse -N pass case but for the values of P_{MAX} just calculated. The number of preamplifiers increases, however, because each pulse which enters the PAM must be at the saturation level. The energy which must be supplied by the preamplifiers is

$$N(PAMs) = N E_{SO} P D^2 / 2$$

where N is the number of pulses to extract energy. The total number of PAMs again assuming that the preamplifiers are identical to the PAMs is

$$N(PAMs) = \begin{cases} \frac{2E_T}{N_{eff} E_{SO} P \left(1 + \frac{2}{3} g_O L_C \right) D^2} \left[1 + \frac{N}{N_{eff} \left(1 + \frac{2}{3} g_O L_C \right)} \right]; & P \leq P_{MAX} \\ \frac{2E_T}{N_{eff} \phi_s D^2} \left[1 + \frac{N E_{SO} P}{N_{eff} \phi_s} \right]; & P > P_{MAX} \end{cases}$$

The total number of PAMs vs pressure is plotted in Figure 114 and the total number is less.

For either multiplex case, the present day S/W characteristics would predict hundreds of PAMs per megajoule on target. Projected S/W numbers still give well over a hundred PAMs per megajoule. There are other alternatives for placement of the S/W but all those considered still give far too many PAMs.

A better concept would be to optimize the PAM configuration independent of the S/W and expand the output beam to the fluence limit of the S/W. The S/W would be constructed as a mosaic for the proper diameter. An estimate for the number of salt discs for the mosaic window concept is

$$N(SALT DISCS) \cong \frac{HW}{D^2} \frac{N_{eff} E_{SO} P \left(1 + \frac{2}{3} g_O L_C \right)}{\phi_s}$$

Assuming a PAM aperture of $H = 1$ m, $W = 2$ m, at 1 atm, ~ 60 salt discs of 45 cm dia. are required for a salt fluence of 1 J/cm^2 . A schematic representation of this S/W mosaic concept is shown in Figure 115. All the multiplexing and amplifications is done within closed ducts which are all at pressure of the PAM cavity. Isolation of the PAM cavity from the beam ducts is accomplished by aerodynamic curtains. After final amplification, the beam is expanded from the PAM fluence to the salt fluence limit and passed through the mosaic. After the mosaic, a series of mirrors catch the beams and refocus it onto a collimating mirror at the mirror fluence limit.

The primary uncertainty of this scheme is in the focusing properties of the beam after being split up into smaller beams, each the size of the salt discs. If phase coherence between the recombined beams is lost, then the spot size at the target will be representative of the salt diameter rather than the diameter of the mosaic. As before, there are two types of optical control, corrective and adaptive, to insure phase coherence among the beams. It is probably economically possible to have the optical thickness of each salt disc equal to each other to within a fraction of a (CO_2) wavelength, plus or minus an integral number of wavelengths. Once fabricated, the mosaic and the optics following it could be tuned to give a coherent phase front at the collimating mirror. In this way, the entire optical system for the S/W is corrective and phase coherence is achieved. To first order, slight motions of the individual salt discs, i.e., translation and tilt, do not affect the phase coherence since the total optical path is not altered by such motions. If, however, thermal or mechanical distortions in the salt discs or the mosaic frame disturb the figure of a salt disc, then phase coherence will be degraded and an adaptive optical system is required.

H. BEAM TRANSPORT TUBES

The beam transport tubes are the structures which contain the laser beam during the multiplex operation and which carry the beam from the multiplex structure to the target chamber area. If salt windows are used as the pressure interface then it is possible to have the beam transport tubes evacuated. In this case, there is no loss of optical beam quality or other attenuations other than portion of the beam lost by mirror scattering, diffraction spreading hitting the walls or otherwise missing the mirror, etc. The mechanical design of the tubes must, of course, accommodate the one atmosphere overpressure and large pumps must be provided to balance any gas leaks into the tubes.

In the aerowindow option, the beam transport tubes are at the same pressure as the PAM cavity (or perhaps at a slightly lower pressure to keep contaminants from entering the PAM cavity)

J1577

Figure 115 Salt Window Mosaic Concept

and the two regions are separated by an aerocurtain, as discussed in this section. With the beam transport tubes at high pressure, the possibility of absorption and/or density fluctuations from thermal convective cells may affect beam quality, especially in view of the long path lengths involved.

The attenuation coefficient for air is $\sim 0.3/\text{km}$ (most of which is due to water vapor) which can result in significant beam attenuation considering a total path length of about 1 km. If the water vapor is removed by proper drying the remaining CO_2 is the principal absorber with an attenuation constant of $\sim 0.3/\text{km/Torr}$. At atmospheric concentrations, 330 ppm, the CO_2 content is $\sim 1/4$ Torr giving an attenuation constant of $\sim 0.08/\text{km}$. At this level the energy absorbed by the gas in the beam transport tubes is $\sim 8 \times 10^{-6} \text{ J/cm}^3$ assuming a mirror limited fluence of 10 J/cm^2 . The temperature rise per pulse as calculated by

$$\Delta T = \frac{8 \times 10^{-6} \text{ J/cm}^2}{\rho C_p}$$

is of the order of $10^{-2} \text{ }^\circ\text{K}$ so that significant thermal buildups can accrue in a multipulse situation. In the steady-state large-scale convection cells would develop to transport hot gas to the walls of the beam transport tube where heat exchange to the exterior can take place.

To avoid these convection cells and the density fluctuations which result, it is advisable to remove the CO_2 from the air as well as the water vapor. Removal of CO_2 can be accomplished by rapidly expanding the flow and freezing out the CO_2 or by standard chemical absorption techniques. Another alternative is to use pure nitrogen as the beam transport tube gas. In this concept leakage of the gas back into the PAM cavity is not a catastrophic fault mechanism. The nitrogen would be derived from the same source which supplies the laser gas.

Even with bulk absorption in the gas held to a negligible amount, density fluctuations could arise as a result of unequal wall heating. An estimate of the maximum allowable density fluctuations is given as follows: The maximum phase front error for a 10% reduction in focal spot intensity is $\Delta\phi \lesssim 0.3 \text{ rad}$. The maximum value of $\Delta\rho/\rho$ for an optical path length of 30 m per (multiplex) pass and a total of 5 passes is

$$\Delta\phi = \frac{2\pi\beta\ell}{\lambda} \frac{\rho}{P_o} \frac{\Delta\rho}{\rho} \sqrt{2N}$$

where

β = Gladstone-Dale constant $\cong 4 \times 10^{-4}$

ℓ = path length per pass (30 m at 1 atm corresponding to a vibrational repump time of 200 ns)

P = beam transport tube pressure

N = number of passes (each pass assumed uncorrelated)

At one atmosphere, $\Delta\rho/\rho \cong 2 \times 10^{-5}$ which will require a high degree of temperature homogeneity at the walls of the beam transport tubes.

If the walls of the beam transport tube are not isothermal, a thermal boundary layer will build up which will remain stable until the Rayleigh number of the layer reaches a value of $\sim 10^3$. At this point, the boundary layer will become locally unstable and transient thermals form, break away from the surface, and mix with the interior volume of the beam transport tube. The thermal masses are random in space and time with an RMS variation of $\sim 10\%$ of the maximum wall temperature inhomogeneity, i.e., $\langle\delta T\rangle_{\text{rms}} \cong 10^{-1} \Delta T_{\text{wall}}$. The thermal nonuniformity in the gas produces density fluctuations in this constant pressure situation of

$$\frac{\langle\delta\rho\rangle_{\text{rms}}}{\rho} = \frac{\langle\delta T\rangle_{\text{rms}}}{T} \cong 10^{-1} \frac{\Delta T_{\text{wall}}}{T}$$

The maximum wall temperature nonuniformity, assuming room temperature operation is then

$$\Delta T_{\text{wall}} \cong 10 \times 300^\circ\text{K} \times 2 \times 10^{-5} = 0.06^\circ\text{K}$$

A wall temperature uniformity of this magnitude will require careful design to insulate the wall from outside temperature gradients. An alternative approach shown schematically in Figure 116 is to actively supply a small amount of heat to the walls to maintain the upper wall at a higher temperature than the lower wall which will suppress the gravity fed thermal instability. An estimate of the heat required to maintain a stabilizing temperature gradient is given as follows: For steel with a heat conductivity of $\sim 10^{-1} \text{ cal s}^{-1} \text{ cm}^{-2}/\text{cm}$, the heat required to maintain a temperature differential of 0.06°K over a 1 m tall beam tube with a wall thickness of 2 cm is $\sim 30 \text{ W/m}$ of length. (It also follows that a similar amount of power is required from the environment to produce the temperature asymmetry in the first place and it should be relatively easy to insulate against such factors.)

BEAM TRANSFER TUBES

ADIABATIC WALL WITH ACTIVE TEMPERATURE CONTROL

J1575

Figure 116 Active Stabilization of Thermal Connective Cells

It is clear that a lower system pressure does not alleviate the problem since the path length increases in the same proportion as the maximum allowable temperature fluctuation decreases. Filling the beam transport tubes with a low refractive index gas, such as helium is quite beneficial. The Gladstone-Dale constant for helium is about a factor of 10 lower and, hence, the maximum allowable temperature uniformity is 10 times larger.

I. LOW PRESSURE A/W

The pellet implosion will release a significant amount of soft x-ray radiation which, if not attenuated, could seriously degrade the performance of the final focussing optics. There appear to be two methods of protecting the final optic from the damaging effects of this radiation, gas refraction and gas absorption. The gas refraction technique shown schematically in Figure 117. The deflection angle for the gas prism is given by

$$1 + \beta = \frac{\sin \frac{1}{2} (\alpha + \delta)}{\sin \frac{1}{2} \alpha}$$

where β = Gladstone-Dale coefficient $\ll 1$. For the small angle approximation, the deflection angle is

$$\delta \cong 2\beta$$

The reactor chamber wall can shield the last mirror if the distance from the mirror to the wall is of the order

$$\frac{2d}{\delta}$$

where d is the diameter of the final mirror. Assuming a value of $\beta \cong 2 \times 10^{-4}$ it appears that the distances involved are too great for such a scheme to be feasible.

The alternative is to provide a mass of gas between the last optic to absorb the soft x-rays as shown conceptually in Figure 118. Soft x-ray energies differ with the various pellet systems. Moreover, the temperature of a given system varies with the time and some of the radiation is not in equilibrium. These temperature ranges can be as high as 2 eV to nearly 1 KeV. For one system⁽¹⁸⁾ at early times one has about 30% of the energy at about 736 eV Blackbody (BB) temperature, 50% at 494 eV (BB) at 2 ns and 20% at 151 eV (BB) at 4 ns. The mean BB temperature for all the x-radiation is 485 eV corresponding to a total energy output of 297 MJ and a total x-ray output of 39 MJ.

J2409

Figure 117 Gas Refraction Cell for Soft X-Ray Shielding

J1519

Figure 118 Low Pressure Gas Absorption for Soft X-Ray Shielding

The mean photon energy for this temperature is 2.7 times greater, i.e., 1310 eV and this is the energy for which the cross sections are given below.

<u>Element</u>	<u>Cross Section BARNS/atom</u>	<u>Cross Section cm²/g</u>
³ Li	1,496	130
7 N	45,000	1,938
18 Ar	130,000	1,964
54 Xe	1,120,000	5,137

all the above is at a photon energy of 1310 eV.

Since the absorption of such soft x-rays in metal mirrors is essentially 100% and it is assumed that the mirrors are already loaded at their maximum fluence limit, very little additional soft x-ray fluence can be tolerated. At an optical fluence level of 10 J/cm², the photon energy absorbed is of the order of 0.1 J/cm². Assuming a background pressure of nitrogen in the chamber, P_O, the soft x-ray flux on mirrors is given by

$$E_T \frac{GY}{XRAY} \frac{1}{4\pi R^2} e^{-\alpha \ell}$$

where

E_T = laser energy on target

G = pellet gain

Y_{XRAY} = soft x-ray yield

R = reactor chamber radius

α = attenuation coefficient of

ℓ = length of the low pressure A/W

The attenuation coefficient for nitrogen at pressure P_O is

$$\alpha = 1938 \frac{\text{cm}^2}{\text{gm}} \times 1.2 \frac{\text{mgm}}{\text{cm}^3} \times P_O \text{ (atm)}$$

or

$$\alpha \approx 2.3 \text{ cm}^{-1} P_O \text{ (atm)}$$

-240-

Plots of the pressure-length product $P_0 l$ as a function of reactor chamber radius for various target energies are shown in Figure 119 for the following pellet parameters

E_T (MJ)	1	3	5
G	130	400	600
Y_{X-RAY}	10%	10%	10%

For a reactor chamber radius of 15 m, the nitrogen pressure ranges from 6-16 Torr for a 1 m thick A/W for 1-5 MJ respectively.

Figure 119 Attenuation Constant of Low Pressure Aerowindows

VII. LASER GAS FLOW

A. INTRODUCTION

The laser gas flow system for the Laser Fusion Driver must circulate the gas in a manner which maintains acceptable limits on density fluctuations within the cavity, at a flow speed which allows sufficient gas interchanges between pulses and without an appreciable pressure drop in the system. The last requirement essentially rules out annular geometrics such as have been built for high energy single-pulsed machines.

The primary reason for discarding annular geometries is the large pressure drop required to insure flow uniformity for the radial flow in the annular region. As shown in Figure 120 the laser gas flows into the inner cylinder of an annular device (from either or both ends) and flows out radially with high pressure drops. The flow power required to overcome this pressure drop in a closed-cycle flow system is

$$P_F \cong \Delta P \times \text{Surface Area} \times \text{Flow Velocity}$$

Assuming, that the flow speed is such as the interchange the laser gas once in the interpulse period, the flow power is given by

$$P_F = 2\pi RL \Delta R (\text{PRF}) \Delta P$$

where

R = average radius of the annular region

ΔR = annular thickness

the average input electrical power to each PAM assuming saturated operation is given by

$$P_{IN} \cong E_s g_o L_c 2\pi R \Delta R (\text{PRF}) / \eta$$

where

E_s = saturation fluence

$g_o L$ = gain length product

η = intrinsic laser efficiency

J2454

Figure 120 Annular Geometry for a Flowing PAM

The ratio of flow power to input electrical power is then

$$\frac{P_F}{P_{IN}} = \frac{\Delta P}{E_s g_o \eta^{-1}}$$

At one-atmosphere pressure and an intrinsic laser efficiency of 10%, a pressure drop of one-atmosphere results in a power ratio of 10 for $E_s = 1/3 \text{ J/cm}^2$ and $g_o = 4\% \text{ cm}^{-1}$. Experience of AERL with large, repetitively pulsed annular CO_2 lasers (run in an open-cycle flow mode) show that at least one-atmosphere pressure drop is required for flow homogeneity. Hence, the power consumed to power the flow loop is larger than the electrical input power and the loss in overall system efficiency for closed-cycle flow is unacceptable. Unless a completely new method of making the transition of axial to radial flow is found which does not require high pressure drops, annular devices are impractical.

In the case of a rectangular PAM geometry, it is simple to show that in carefully designed systems the pressure drop which is needed for flow homogeneity is small and the power consumed in the flow system is a small fraction of the input power. The circulating or "reactive" power associated with the laser gas flow in the PAM cavity is

$$P_F = \frac{1}{2} \rho V^3 L_c H$$

while the input electrical energy is

$$P_{IN} = E_s g_o L_c \eta^{-1} \text{ HW (PRF)}$$

Assuming the gas velocity is based upon one flow interchange per interpulse period, the ratio of circulating to input electrical energy is

$$\frac{P_F}{P_{IN}} = \frac{\eta}{2} \frac{\rho V^2}{E_s g_o}$$

For an efficiency of 10%, a Mach number of 1/4 and gain of $4\% \text{ cm}^{-1}$ the power ratio is $\sim 2\%$. Thus, if one dynamic head were lost in maintaining good medium homogeneity the power invested in the flow system is a small percentage of the electrical input energy. In the following sections the pressure drops necessary to achieve good optical quality will be investigated to make these estimates quantitative.

B. OPTICAL QUALITY

The optical quality of the output beam from a PAM depends upon the density fluctuations in the gain medium inasmuch as these perturbations produce variations in the refractive index. In the usual derivation, the aberration in the phase front of the beam is calculated by computing the phase difference, $\Delta\phi$, between two rays passing through a laser gain medium at two locations in the plane transverse to the laser axis. The wave vector in the gain medium is related to the free space wave vector by the refractive index, n as

$$K = K_0/n = K_0/(1+\beta)$$

where

$$\beta = \text{Gladstone-Dale constant}$$

The constant β is proportional to pressure and is usually specified at a base or reference pressure so that

$$\beta = \beta_0 \frac{\rho}{\rho_0}$$

The phase aberration is then

$$\Delta\phi = \frac{2\pi}{\lambda_0} \beta_0 L \frac{\rho}{\rho_0} \frac{\Delta\rho}{\rho}$$

This derivation assumes that the density variations along the optical path are all ordered and, hence, add coherently. In practice, the phase variations may result from turbulence in which case the derivation is slightly different.

For random variations, it is assumed that the scale size of the turbulence, Λ , is much smaller than the total path length and that the phase variations add incoherently according to a "random walk" dependence. Also, the rms fluctuations are generally no > 20% of the average in which case the phase difference suffered by two rays across one turbulent scale is

$$(\Delta\phi)_\Lambda = \frac{2\pi}{\lambda_0} \Lambda \frac{\rho}{\rho_0} 20\% \Delta\rho/\rho$$

the total phase variation is then multiplied by the square root of the number of turbulent masses, i.e.,

$$\Delta\phi = \sum_L (\Delta\phi)_L = (\Delta\phi)_L \sqrt{L/\Lambda}$$

$$= \frac{2\pi}{\lambda_0} \beta_0 \frac{\rho}{\rho_0} 20\% \frac{\Delta\rho}{\rho} \sqrt{L/\Lambda}$$

The importance of average and random phase variations is evident from the ratio

$$\frac{(\Delta\phi)_{\text{AVG}}}{(\Delta\phi)_{\text{RANDOM}}} = \frac{1}{20\%} \sqrt{L/\Lambda}$$

For large CO₂ lasers with well-designed flow systems and screens to set the turbulent scale size, the ratio of L/Λ can be of the order of 100 so that the ratio of average to random phase variations is of order 50. Thus, the random phase errors are less important than the average variations but are more difficult to correct. Average density variations have small spatial and temporal gradients and their effects may be correctable by fixed optical elements or slowly varying adaptive control systems. Random fluctuations are of smaller scale and move rapidly, usually with the sound speed and, hence, are only correctable by high spatial and temporal frequency actuators on "rubber mirrors." For example, room temperature turbulence with centimeter scale size require actuators with tens of kilohertz bandwidth for adaptive control systems.

For a coherent laser beam with phase aberration, the decrease in on-axis intensity in the farfield is given by the "Strehl ratio"

$$\frac{I(\Delta\phi)}{I(\Delta\phi = 0)} = \exp(-\Delta\phi)^2$$

where the maximum allowable phase variation per meter of gain length is estimated by the worst case assumption, i.e., non-random. For a 10% loss in on-axis intensity

$$\frac{I(\Delta\phi)}{I(0)} = 0.9 = \exp(-\Delta\phi^2)$$

or

$$\frac{\Delta I}{I(0)} = 0.1 \cong \Delta\phi^2$$

Solving for $\Delta\rho/\rho = 1.3 \times 10^{-3}$ per meter for a pressure of 1 atm and $\beta = 4 \times 10^{-4}$.

There are two aspects of the control of density variations, the homogeneity of the base flow directly before initiation of the discharge and the uniformity with which the electrical energy is loaded into the gas. With regard to the base flow uniformity, the plot of Figure 121 summarizes the requirements on $\Delta\rho/\rho$ for CO₂ lasers as a function of gain length for a single pass through the amplifier based upon ordered disturbances. Present state of the art for large CO₂ lasers is between 10^{-3} and 10^{-4} permitting several meters of gain length for near diffraction limited performance. Recent experiments conducted with the goal of improving the state of the art for visible laser beam quality have demonstrated $\Delta\rho/\rho < 5 \times 10^{-5}$ (19) on small-scale flow geometries. The homogeneity of the baseline flow is demonstrated in Figures 122 and 123. On a small-scale test bed, the interferograms in Figures 122 and 123 show the baseline homogeneity before discharge initiation with and without flow present. Computer reduction of these interferograms show that the average density variation is $< 5 \times 10^{-5}$.

The effect of flow on the acoustic clearing time is illustrated in Figures 124 and 125 which show the flow field uniformity prior to and 2 ms after discharge initiation. In this case the Mach number was 0.2 and the 2 ms clearing time corresponds to a pulse repetition rate in excess of 100 Hz.

These results indicate that large laser cavities can be constructed with sufficient acoustic control to permit near diffraction limited beam quality at high pulse repetition rates. The analysis used thus far, has been the worst case estimate, i.e., ordered disturbances as opposed to random fluctuations. For multipass configurations, it would be unlikely for the density variations per pass to add coherently even though this might be the case on a single pass. Hence, the conditions for multipass amplification is

$$\Delta\phi = \frac{2\pi\beta OL}{\lambda} \frac{\rho}{\rho_0} \frac{\Delta\rho}{\rho} \sqrt{N}$$

where N is the number of passes.

The second aspect of density variations is pulse heating, i.e., can inhomogeneities of the input energy cause density variations during the pulse which cause significant phase aberrations. If the input energy is loaded into the cavity with perfect uniformity, the only density gradients which can occur within the mode volume are those which travel in from the edges. These density disturbances travel at approximately the sound speed and, hence, take many tens of microseconds to travel in from the edges. However,

Figure 121 Allowable Density Variations for CO₂ Lasers

Figure 122 Initiation Tare Shot (no flow)

Figure 123 Baseline Flow Field ($M = 0.2$)

Figure 124 Flow Field Uniformity Prior to Discharge Initiation

Figure 125 Flow Field Uniformity 2 ms after Discharge Initiation

when the input energy loading is inhomogeneous, local disturbances expand and the process is one of inertial relaxation rather than transit time at acoustic speeds. These processes are well known in the study of laser propagation through the atmosphere since they affect the degree of "thermal blooming." The equation relating the scale length of input energy inhomogeneities to density variations at the end of the input pulse is

$$\frac{\Delta \rho}{\rho} = \frac{\gamma-1}{\gamma} \frac{(a t_p)^2}{6} \frac{Q_{IN}}{P} \frac{1}{\Lambda^2}$$

where

a = sound speed

t_p = pulse duration

Q_{IN} = input energy density

P = cavity pressure

Λ = inhomogeneity scale length = $\left(\frac{1}{Q} \frac{dQ}{dx} \right)^{-1}$

A plot of the minimum scale length for 1.1 times diffraction limited beams, i.e., $\Delta I/I = 10\%$, is shown in Figure 126 for a 3 m long PAM and up to 10 multipasses through the PAM. The square root dependence of the number of passes is used. Figure 127 shows an interferogram of a high energy CO_2 pulsed laser taken at 19 μs after the pulse was turned on. Aside from the drop at the cathode layer, no fringes can be seen in the bulk of the mode volume and the homogeneity of the input power is excellent.

C. FLOW TUNNEL DESIGN

The design of the laser gas flow tunnel is a critical part of the overall system primarily in terms of its cost. Wind tunnel designs which are efficient in that they require low electrical power to drive them tend to be bulky and expensive to construct. It is most important, therefore, to optimize the design by trading off power efficiency against construction costs. In the most basic configuration, a PAM is built into the test section of high speed, subsonic wind tunnel as shown in Figure 128. Downstream of the test section is a diffuser which recovers a portion of the pressure head. Vanes turn the flow and for convenience, the diffuser is split into two sections. A fan located in the circular section of the duct powers the flow and overcomes the pressure drops caused by the vanes, heat exchanger and flow conditioning screens. The

J1468

Figure 126 Density Variations Caused by Pulse Heating

Figure 127 Medium Homogeneity During the Pulse of an E-Beam Sustained Discharge Laser

100 KJ LASER MODULE GAS RECIRCULATOR FOR ATM CAVITY
1 x 2 x 10m

J2402

Figure 128 Wind Tunnel Concept for a PAM

tunnel depicted in Figure 128 is approximately to scale and assumes a PAM cavity of length two meters in the flow direction and one meter in the e-beam direction. An area contraction ratio of 6 insures efficient operation.

Estimates of the pressure drops around the flow loop are expressed in terms of the local "Q" where

$$Q = \frac{\Delta P}{\frac{1}{2} \rho_c v_c^2}$$

where the subscript "C" refers to cavity conditions. The Q factor represents the number of local dynamic pressure heads lost by a flow element. Since $\rho = \rho_c$ for this subsonic tunnel, the continuity relation

$$\rho v A = \rho_c v_c A_c$$

gives the relation

$$v/v_c = A_c/A$$

and

$$(v/v_c)^2 = (A_c/A)^2$$

Hence the ratio of the pressure drops is

$$Q/Q_c = (A_c/A)^2$$

Estimates of the wind tunnel components are as follows:

1b Heat Exchangers:

The local pressure drop for heat exchangers such as these used on a high-power cw CO₂ laser is

$$Q = 5-10$$

$$Q_c = 14-28\%$$

2b Screens:

To insure velocity uniformity of 1 part in 10⁴, fine mesh screens are needed. Estimates of the pressure drop in such screens are

$$Q = 2-3$$

$$Q_c = 6-8\%$$

3) Turning Vanes:

In the present design configuration, two of the turning vanes are already in the large section of the tunnel but the remaining two are in the intermediate area section. For efficient diffusers, the maximum expansion angle is limited by flow separation to $< 5^\circ$. Choosing a diffuser length of 25 m, the cross sectional area of the tunnel at the exit of the diffuser is 6 m x 10 m, assuming the entire expansion ratio is taken in the height dimension. Local pressure drops for the turning vanes are estimated at $Q = 0.15/\text{vane}$ so that the pressure drop referenced to the test section is

$$Q_c = 0.15 \times 2/3^2 + 0.15 \times 2/6^2 = 4\%$$

4) Diffuser:

The pressure drop in the diffuser is given by

$$Q_c = (1 - \eta_d) [A/A_d]^2]$$

where

η_d = diffuser efficiency

A_c = PAM cavity area

A_d = area of exit of diffuser

Assuming a diffuser efficiency of 90% and an area ratio of $A_d/A_c = 6$, i.e., both halves of the diffuser are combined, the diffuser loss is

$$Q_c = 10\%$$

Summing the total losses for the flow loop,

$$Q_{CT} = \sum_c Q_c = \frac{\Sigma \Delta P}{\frac{1}{2} \rho_c v_c^2} = 34-50\%$$

The total power required to power the flow loop is then

$$P_F = v_c A_c \Sigma \Delta P = \frac{1}{2} \rho_c v_c^3 A_c \Sigma Q_c$$

For the representative example of a PAM cavity of dimensions 1 m x 2 m x 10 m, PAM pressure and flow Mach number of 1 and 0.2 atm, respectively, the flow power is

$$P_F = \left(\frac{1}{2}\right) \left(1 \frac{\text{kg}}{\text{m}^3}\right) \left(300 \frac{\text{m}}{\text{s}} \times 0.2\right)^3 2\text{m} \times 10\text{m} \times 50\% = 1\text{ MW}$$

Such a PAM cavity would be capable of 100 kJ/pulse or an average power of 1 MW at 10 Hz. A flow power of 1 MW represents a small fraction of the input electrical power and is reasonable.

The power consumed by the flow tunnel is proportional to the cube of the Mach number

$$P_F = (\Sigma Q_C) \frac{1}{2} \rho_C a^3 M^3 L_C H$$

whereas the output laser power is given by

$$P_L = N_{\text{eff}} E_s g_O L_C HW (\text{PRF})$$

However, the complete relation for acoustic clearing is

$$\frac{W}{a} \text{ PRF} = M(1-M)$$

which indicates that the laser power scales approximately as M for $M \ll 1$. The ratio of flow power to laser input power is plotted in Figure 129 and for $M \gtrsim 0.4$, the flow power costs are prohibitive. The power ratio optimizes at low M whereas other system variables, e.g., A/W , optimize at high M . The power to run an A/W is independent of cavity Mach number and, therefore, higher values of M for higher PRF give a more efficient use of A/W power.

For $M \lesssim 0.4$, the primary concern from a systems point-of-view is capital cost and not the electric power consumed to operate the flow tunnel. High capital costs are associated with the design of Figure 128 because of the small angle diffuser and large contraction ratio. Both factor combines to give a long, large diameter wind tunnel. An example of how this design might be reduced in size and volume is shown in Figure 130. Due to the low value of pressure drop across the laser channel, several PAMs can be staged in series before a fan or compressor is used. Secondly, the shallow angle constraint of the diffuser is alleviated for low Mach numbers by placing the heat exchanger and flow straightening screens directly at the diffuser output. In this way, the diffuser works into a higher "back pressure" which results in a favorable pressure gradient in the diffuser section. Expansion angles of almost 40° can be accommodated without boundary layer separation with this technique.

Figure 129 Ratio of Flow Power Consumption to Input Power

CONTRACTION RATIO

(3.33 CH)
COMPRESSOR DRIVE
SECTION

(4CH + 2W + 0.5H)
LASER SECTION

J1444

Figure 130 Compact Flow System Concept

The local Q , due to the heat exchange screen for this design can be expressed as

$$Q_{HX} \cong 2.76 + 7.18 \ln \left(1 + \frac{\Delta T_{cav}}{\Delta T_{cool}} \right)$$

where

ΔT_{cav} = temperature use of the laser gas after lasing

ΔT_{cool} = temperature difference between the PAM inlet temperature and the cooling fluid temperature.

The quantity ΔT_{cav} is given by

$$\Delta T_{cav} = Q_{input} (1 - \eta_L) / \rho C_p$$

where

Q_{input} = input energy density into the gas

η_L = laser efficiency

For example, for an input energy density of 100 J/L, laser efficiency of 10%, mass density of 1.7 g/L and $c_p = 1$ J/g°K, the rise in cavity temperature is only ~ 50°K. Similarly for a coolant temperature of 30°K, the value of $\Delta T_{cool} \cong 20^\circ K$ giving a local Q_{HX} of ~ 10. This value is the upper limit for the range of heat exchanger values previously quoted and, hence, less efficient, but the design is much more compact and less expensive to construct.

The local Q can be referenced to the cavity pressure as follows:

$$\frac{P}{P_c} = Q_{HX} \frac{\left(\frac{1}{2} \rho v^2 \right)}{P_c}$$

where

v = local velocity

P_c = cavity pressure

Using the flow continuity equation as before with assuming that $\rho = \rho_c$ for subsonic flow, the pressure drop becomes

$$\frac{\Delta P}{P_c} = \frac{Q_{HX}}{P_c C^2} \frac{1}{2} \rho_c v_c^2$$

where

C = area contraction ratio

Using the relations

$$P_c = \rho RT$$

$$v_c = ma$$

$$a = \gamma RT$$

the final form is obtained,

$$\frac{P}{P_c} = \frac{Q_{HX}}{C^2} \frac{\gamma}{2} M^2$$

The pressure drop caused by the heat exchanger is probably the largest single quantity. The pressure drop applies to the heat exchanger-diffuser combination since for most practical designs, the heat exchanger loss dominates. The heat exchanger when placed at the end of the diffuser causes a back pressure which allows the diffuser to operate with large expansion angles without flow separation. In this way the efficiency of the diffuser is increased. When the area contraction ratio becomes very large, the heat exchanger loss drops but the diffuser losses now increase. Thus, for large values of C , the pressure drop of the heat exchanger-diffuser combination is

$$\frac{\Delta P}{P_c} = \text{MAXIMUM} \left[\frac{Q_{HX}}{C^2} ; (1-C^2) \frac{\gamma}{2} M^2 \right]$$

where the larger of the two factors is chosen. The significance of this pressure ratio is that it fixes the number of PAMs that can be staged in a series before a compressor or fan is required to make up the total pressure drop. A plot of the maximum number of PAMs per fan as a function of M and C is shown in Figure 131, where a maximum of 10% pressure drop is allowed before recompressing.

The complete design of the flow tunnel requires a detailed engineering evaluation of the heat-exchanger, cavity diffuser and fan. A simplified set of scaling laws is presented instead to

Figure 131 Maximum Number of PAMS per Compressor Module

aid in formulating tradeoff comparisons for the overall system. A flow module would comprise a PAM cavity, muffler, diffuser-heat exchanger combination, and contraction ratio sections. The length of the flow module is shown schematically in Figure 130.

- a. PAM Cavity: The length of the cavity is given by the flow dimension, W.
- b. Muffler: Two muffler sections are generally required to damp out acoustic disturbances and prevent reflections back into the cavity. The total length of the up and downstream muffler sections is approximately W.
- c. Diffuser: The "streamline" diffuser section used to low Mach numbers requires a length of $\sim 3 CH$ where H is the cavity dimension in the e-beam direction.
- d. Heat Exchanger: The length of the heat exchanger determined from semi-empirical design data in meters, is

$$W_{HX} = 0.27 n \left(1 + \frac{T_{cav}}{T_{cool}} \right) \left(\frac{M}{C} \right)^{1/4} P_c^{1/4} \left(\frac{m}{40} \right)^{1/8} \left(\frac{T_o}{300} \right)^{-0.35}$$

where

$\Delta T_{cav}/\Delta T_{cool}$ = ratio of cavity to cooling temperature rise

M = cavity Mach number

C = area contraction ratio

P_c = cavity pressure (atm)

m = molecular weight of laser gas

T_o = laser gas inlet temperature ($^{\circ}K$)

The length of the heat exchanger can be quite small. For temperature rises of $\Delta T_{cav} = 80^{\circ}K$ and $\Delta T_{cool} = 50^{\circ}K$, cavity Mach number of 0.1, contraction ratio of 3 and 1 atm operation, the length of the heat exchanger is only ~ 0.1 m.

- e. Contraction Section: The contraction from the full tunnel diameter to the PAM cavity can be quite rapid. $\sim CH$.

The total length of a flow module is then $\sim 4 CH + 2 W + W_{HX}$.

The compressor section consists of an inlet nozzle, the compressor itself and a diffuser with lengths of $\sim 2/3$ CH, $2/3$ CH and 2 CH respectively for a total length of ~ 10.3 CH. In all these designs, it is assumed that all the area expansion is taken in the height of the flow duct and that the width of the flow duct, L_c , remains constant. In most designs $L_c > H$ by a factor of ~ 3 so that an expansion, $C \cong 3$, makes the expanded cross section almost square.

Each compressor unit has a pressure drop associated with it and a heat exchanger is needed to remove heat added to the gas by the compressor. The total pressure drop which must be supplied by the compressor is

$$N \frac{\Delta P}{P_c} + \frac{\Delta P_{FAN}}{P_c}$$

where

N = number of flow modules per compressor unit

$\Delta P/P_c$ = pressure drop per flow module

ΔP_{FAN} = pressure drop caused by the fan heat exchanger

The expression for the fan pressure drop due to the heat exchanger is similar to the previous heat exchanger relation

$$\frac{\Delta P_{FAN}}{P_c} = \frac{\gamma}{2C^2} M^2 \left[2.76 + 7.2 \ln \left(1 + \frac{\Delta T_{FAN}}{\Delta T_{cool}} \right) \right]$$

where

$$T_{FAN} = 0.29 T_o \left(N \frac{\Delta P}{P_c} + \frac{\Delta P_{FAN}}{P_c} \right)$$

the relations for P_{FAN} and T_{FAN} contain each other and, hence, must be solved in an iterative fashion.

Large-scale fans or compressors are well developed at present, the most common appearing in large jet and rocket engines. For example, the turbofan from a Boeing 747 jet is shown schematically in Figure 132 with a fan diameter of ~ 2 m. Most fans of this type as well as other smaller units for smaller jet engines can supply a pressure drop of 10-20% at one atmosphere. Their ruggedness and reliability is well documented. A brief survey of the performance specifications of these fan units shows an order of magnitude price of $\sim \$2/\text{CFM}$ at 10-20% pressure drop and in the million CFM range.

CF6 - 50 A (BOEING 747 TURBOFAN ENGINE)
COST OF FAN + SHAFT + TURBINE - \$ 800,000

H6281

Figure 132 Outline of a Large Scale Fan Compressor

D. OVERALL TUNNEL CONFIGURATION

Some degree of module grouping is desirable since, the pressure drops of several PAMs can be supplied by a single compressor unit. In this way, the individual transition units and return flow loop ducts are eliminated. A natural extension of this series grouping is to stack all the compressors and flow modules in series in the form of a torus as shown in Figure 133. In this illustration, two flow modules can be grouped per compressor. In Figure 134 the beam multiplex tubes are shown as radial spokes followed by beam transport tubes which lead to a central A/N and followed by the reactor chamber (not shown but presumably located below the A/W).

This concept of a single toroidal flow loop may not be the optimum design when considerations such as reliability and accessibility are taken into account. In case of PAM failure, the discharge could be stopped and that module left inactive until a convenient shut down time. (This presumes an extra number of PAMs whose number depends upon the results of reliability analysis. In case of compressor failure, the blades could be feathered to reduce drag and enough PAM discharges turned off to accommodate the lower total compressor values. Other concepts such as bypass ducts could isolate a defective unit, either PAM or compressor, however, the extra construction costs may be large. Finally, for Fusion Driver System larger than ~ 5 mJ, the radius of a single toroidal flow loop becomes so large that costs of the beam transport tubes become prohibitive. At this point it is advantageous to build several loops.

E. AEROCURTAINS AND MAKEUP GAS FLOW

Whether aerowindows or saltwindows are used for the pressure interface between the PAM (high pressure) and the reactor (low pressure) chamber, an aerocurtain is required to keep laser gas from leaking into the multiplex tubes. A schematic representation of the flow involved in an A/C is shown in Figure 135. The A/C gas has the same composition as the gas fill for the multiplex tube. The main criterion for this gas is that it should non-absorbing at CO_2 wavelengths. Water vapor and CO_2 are, therefore, ruled out as constituents and, hence, if air is used, these gases must be eliminated by CO_2 absorption and drying. Air is a natural choice because of the obvious economics involved, but it has a drawback that if air accidentally leaks into the laser cavity, the O_2 will poison the discharge. Pure N_2 as derived from the boiloff of liquid N_2 may be best choice because of its purity and absence of IR absorption.

J1574

Figure 133 PAM and Compressor Staging in a Toroidal Flow Loop

Figure 134 Multiplex Beam Tubes

J2403

Figure 135 Mixing Layers with and without Shear in an Aerocurtain Configuration

Other configurations of A/C flow are shown in Figure 136. In Figure 136(a), the entire mixing layer is bled off in the A/C exhaust duct and, hence a portion of the laser gas flow must be resupplied. In Figure 136(b), the mixing layer is bled off into the laser gas exhaust duct and no laser makeup is required but the laser gas must be purified or reconstituted to the proper mixture ratio. In either case, the volume of gas to be either made up or reprocessed must be kept at a minimum because of the large quantities of gas involved. For example, the total laser gas flow at an output energy density of 10 J/l , target energy of 1 MJ and repetition rate of 10 Hz is 10^6 l/s or 60 tons/min so that even small makeup rates are significant.

The makeup rate is determined by the thickness of the turbulent mixing layer. Even when the shear in the bulk flow is zero, i.e., matched velocities, there is a wake which develops off the splitter plate as shown in Figure 137. The design of the splitter plate is crucial to the growth of the wake. If the splitter plate is too long and thin (Figure 137(b)), a thick boundary layer develops along the plate and grows as $\sqrt{x}$ where x is the distance along the plate. If the splitter plate angle is too large, flow separation will occur in the adverse pressure gradient (Figure 137(c)). For a properly designed splitter plate where the opening angle is kept below $\sim 5^\circ$ and the dimensions in the flow direction kept as small as mechanical considerations will permit, the mixing layer and species concentration are as shown in Figure 137(d). The definition of the width of the mixing layer, $\delta(x)$ corresponds to about a 5% contamination level of one species into the other. Clearly this mixing layer must be kept as thin as possible. The thinnest mixing layer is laminar and that is the desired goal.

In recent experiments using an A/C to isolate laser gas media from saturable absorbers, the concentration profile was measured using trace gases (SF_6 and $10.6 \text{ laser absorption}$) and also by simulating species diffusion by thermal diffusion. The results are shown in Figure 96. At a flow distance of 35 cm the mixing layer thickness has grown to 3 cm or 10% . This 10% growth is approximately the same even for small amounts of shear. The thickness of the mixing layer is greatly reduced by placing a small mesh screen over the splitter plate. The reason for this reduction in mixing layer thickness stems from the dependence of eddy diffusivity upon the turbulence scale size:

$$\epsilon = \tilde{v} \ell$$

where

$\tilde{v}$ = fluctuation level of the velocity

ℓ = turbulence scale length

-273-

J1515

Figure 136 Aerocurtain Options

MIXING LAYER GEOMETRY
(a)

SPLITTER DESIGN
(c)

SPLITTER DESIGN
(b)

CONCENTRATION PROFILES AT X
(d)

VELOCITY PROFILE
(e)

J2406

Figure 137 Wake Geometries for Aerowindows

In the case of zero shear, the velocity fluctuations are caused by the wake defect as shown in Figure 137(e). In this case, it is found that

$$v = 20\% \Delta v$$

where

$$\Delta v = \text{wake defect}$$

By placing a screen over the splitter plate, the turbulence scale is reduced to the mesh size, the diffusivity is reduced and the thickness of the mixing layer is decreased as shown in the lower curve of Figure 96. The pressure drop of high mesh screens is given by

$$\frac{\Delta P}{P_c} = Q \frac{\gamma M^2}{2}$$

where

$$Q = (\ell/d)^2$$

ℓ = reciprocal of the mesh size

d = wire size

Force 200 mesh screen of the type used in Figure 96, the ratio $L/d = 2$ and $Q = 4$. The pressure drop across such a screen at $M = 0.2$ is 10% of the cavity pressure. Such high loss factors can only be tolerated over limited cross sectional areas.

It may also be possible to keep the mixing layer thin or even laminar by active methods. In Figure 138 two active methods of boundary layer control are illustrated. The boundary layer can be eliminated by suction or the wake defect can be removed by a jet of gas.

The makeup rate is given by the ratio

$$\sim \frac{1}{2} \delta / L_c$$

where

δ = mixing layer thickness

L_c = PAM length in the optical direction.

For a mixing layer thickness of 1 cm and gain length of 3 m, ~15% of the main flow must be either made up or reprocessed.

BOUNDARY LAYER
SUCTION

CENTER JET

J2405

Figure 138 Active Methods of Boundary Layer Control

The exact amount of the makeup rate depends upon placement of the catcher plate. In Figure 139, the position of the catcher plate depends upon the maximum acceptable amount of laser gas contamination by A/C gas, and hence the profile of the species concentrations. The analytic form of the species profiles is analogous to thermal diffusion in a semi-infinite solid i.e., and error function. The species concentration profiles are plotted in Figure 139. The experimentally determined value of the mixing layer thickness, δ , is found by extrapolating the straight line fit to the error function curve corresponding to the 90% point in laser gas or the 10% point in the A/C gas. If the contamination of laser gas by A/C gas were to be reduced from 10% to $\sim 2.5\%$ the half width of the mixing layer must be increased by $1.3/0.9$ or $\sim 40\%$

F. FLOW LOOP CONSTRUCTION

The construction costs of the flow loop as well as the multiplex and beam transport tubes represents a substantial fraction of the total system costs for the Laser Fusion Driver. The tunnels, already of substantial size, must not only be strong enough to support their own weight, but also must withstand any pressure difference caused by an operating pressure other than one atmosphere. The laser flow tunnel has only a small section at reduced diameter, the bulk of the flow tunnel is at the expanded cross section which is $\sim L_c \times CH$. The diagonal of this rectangular cross section is ~ 4 m for typical large-scale PAM designs. Since only a small length is at the reduced cross section, a cost efficient approach would be to fabricate a flow tunnel of constant cross section as the basic pressure vessel and to construct a thin-skinned shell inside to achieve the exact contour of the PAM, diffuser, contraction section, etc.

There are several methods of construction for constant area tunnels including precast concrete tubes, cast-in-place concrete slabs, welded steel plate, etc. The appropriate material and construction technique depends upon the tunnel cross-sectional shape, pressure difference, thermal insulation requirements, etc. Without specifying construction details of a complete system, estimates and a scaling law of construction costing is needed, if only in a relative sense, to trade off various PAM design options.

The simplest approach is to formulate a volumetric cost which includes cost of material, on site delivery and construction labor. The volume of material required for a flow tunnel (or multiplex tube) is found by multiplying the perimeter, length and thickness of the tunnel walls. The first two quantities are determined by the PAM design of the previous sections and the thickness by civil engineering formulae relating construction weights, pressure differences and the strength of materials. In this spirit, a construction manual gave present day volumetric costs of \$356/cu yd precast concrete. These costs included materials, re-enforcing rod, labor and delivery. In a similar spirit the cost of welding sheet steel into large ducts (of simple cross section) was estimated at \$1.60/lb.

278-13

J1513

Figure 139 Species Profiles in a Turbulent Mixing Layer

A more complete estimate would explicitly break out sight preparation, cost of material, installation of forms, surface coatings, etc. With this type of complete analysis the major cost items can be identified and the tunnel design optimized for lowest cost. For example, in a study performed by the architectural and engineering firm of Burns and Roe for a reactor containment building, a significant cost item was the form charge for the concrete building. Although all the categories of charge reflected the high cost associated with nuclear containment structures, the relative values were illustrative. The installation charge for the concrete forms dominated the cost of the flow tunnel and multiplex tubes because of the high surface area to volume ratio of these structures as compared to that of a containment building with massively thick walls. Noting this feature of the cost breakdown, cost savings might be realized by switching to a precast, circular cross-section tunnel fabricated in sections in the manner of a tunnel or aquaduct with the sections mated together and sealed on sight. Similar comments apply with respect to steel, ferrocement, fiberglass or other materials.

VIII. FUNCTIONAL DESCRIPTION OF SYSTEMS CODE

A. INTRODUCTION

In each of the subsystems which comprise the Laser Fusion Driver, there are numerous design options to consider, each of which may have a significant impact on the efficiency and/or the total system cost. In order to keep track of the inter-relationships of these design options a computer code was constructed along the lines indicated in Figure 140. The intent of this code is not to provide a complete system design but rather to allow sensitivity studies whereby once a system design is chosen, the effect of varying a subsystem on the total system efficiency and cost can be calculated. In this sense, such subsystem sensitivities should be viewed in a relative rather than an absolute sense since, for example, pricing estimates are only rough estimates.

The code was constructed by choosing a particular design within each subsystem. Efforts were made to optimize the particular design choice within each subsystem, however, a complete optimization would require variation of each subsystem design choice as it relates to the entire system. Once the subsystem design was chosen, its performance was characterized by a simple set of algorithms or tables. It was not possible in all cases to completely characterize the subsystem by this approach and in these cases, the input and output parameters were linked by a simple analytical mode.

B. CODE INPUTS

There are two categories of code inputs, the intrinsic laser medium values and the extrinsic system parameters. The intrinsic properties of the laser medium are derived from the AERL kinetics code described in Section III and listed specifically in Table I. The laser gas pressure, (e-beam controlled) electron density and $N_2:CO_2$ mixture ratio are specified and the kinetics code computes the optimum value of electric field and thus the optimum pumping parameters. The efficiency of pumping the laser mixture is time dependent and an optimum pumping time exists. The optimum pumping time is rather broad and generally occurs at low values of input energy. To obtain higher energy inputs at little sacrifice to efficiency, all parameters are specified slightly beyond the optimum, at 90% of that peak value. At that operating point, the following quantities are specified:

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Figure 140 Block Diagram of Fusion Driver Systems Code

1. input energy density (J/liter)
2. electric field (kV/cm)
3. small signal gain (%/cm)
4. discharge current density (A/cm^2)
5. pumping time to 90% of optimum (μs)
6. pumping efficiency (at the 90% point), $g_o E_{\text{SAT}}$ /input power. Note that this definition of pumping efficiency is not the output nor the first pulse efficiency since the factor of 1/2 or 2/3 corresponding to one or two band extraction is missing as is the multiple pulse effect. The quantity $g_o E_{\text{SAT}}$ is the product of the photon energy and the inversion density so that $g_o E_{\text{SAT}}$ represents the maximum energy stored in the population inversion before single- or multiple-pulse extraction.

A partial listing of the input variables is shown in Figure 141. The following comments will explain these choices.

1. Three mixtures were investigated in the kinetics code, (1/4:1), (1/2:1) and (1:1) for the $\text{N}_2:\text{CO}_2$ ratio. The specific mixture ratio is important in determining the performance of the multipulse optical extraction scheme, the flow properties and makeup rates.
2. Three laser gas pressures were investigated, 1/2, 1 and 2 atm. Low gas pressures allow for long electrical pumping times but lead to large volume devices. The opposite is true for the high pressure region.
3. The electron density is varied in 4 steps

$$\frac{n_e}{P} = 1, 2, 5, 10 \times 10^{12} \text{ electrons/cm}^3/\text{atm}$$

Low values of electron density give slower pumping times but lower specific energy inputs.

4. The total discharge voltage was varied in 3 steps, 1/2, 3/4 and 1 MV. For efficient energy transfer, the charge voltage on the PFN is twice the operating voltage and 1 MV represents the present state of the art in bushing technology. Near term advances should push this limit to multimegavolt system.

FUSION DRIVER SYSTEMS CODE

A. INPUTS (PARTIAL LISTING)

1. LASER GAS MIXTURE	$N_2/CO_2 - 1/1 - .25/1$
2. CAVITY PRESSURE	0.5 - 2.0 ATM
3. ELECTRON DENSITY/PRESSURE	$10^{12} - 10^{13} \text{ cm}^{-3} \text{ atm}^{-1}$
4. TOTAL DISCHARGE VOLTAGE	0.5 - 1.0 MV
5. LASER GAS INLET TEMPERATURE	300°K
6. SALT WINDOW COST	\$15 cm^{-2}
7. SALT WINDOW FLUENCE	1 - 2 J cm^{-2}
8. SALT WINDOW DIAMETER	45 - 60 cm^2
9. $g_o L_c / g_o L$	0.7
10. THYRATRON VOLTAGE	200 kV
11. THYRATRON PEAK CURRENT	20 KAMPS
12. THYRATRON LIFETIME	1 YEAR
13. IGNITRON VOLTAGE	25 kV

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Figure 141a List of Input Variables to the Systems Code

14. MAXIMUM IGNITRON COULOMB TRANSFER	200 COUL.
15. MAXIMUM IGNITRON PEAK CURRENT	300 KAMPS
16. PULSE TRANSFORMER COST	\$10K
17. PULSE TRANSFORMER TURNS RATIO	10/1
18. PULSE TRANSFORMER AVERAGE POWER	1.67 MW
19. CAPACITOR COST	\$0.40 JOULE ⁻¹
20. CAPACITOR LIFETIME	1 YEAR
21. POWER SUPPLY COST	\$9 kW ⁻¹
22. COSTS ON MISC ELECTRICAL PARTS	LIST
23. CONCRETE COST	\$356 CUYD ⁻¹
24. STEEL COST	\$1.60 lb ⁻¹
25. THERMIONIC EMITTER COST	\$2 cm ⁻²
26. MIRROR COST	\$15 KM ⁻²
27. MIRROR MOUNT COST	\$20 KM ⁻¹

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Figure 141b List of Input Variables to the Systems Code

28. BUSHING COST	\$20K
29. NUMBER BUSHINGS PAM ⁻¹	2
30. BUILDING COST	\$10 ft ⁻²
31. TURBO PUMP COST	\$5900
32. PELLET GAIN	65-130 AT 1 MJ 200-400 AT 3 MJ 300-600 AT 5 MJ
33. TARGET ENERGY	1, 3, 5 MJ
34. PRF	2 - 40 Hz
35. AERO WINDOW BREAKDOWN FLUX	10 ⁹ - 10 ¹⁰ Wcm ⁻² (SEE CURVE)
36. AERO WINDOW FLOW POWER	15 MWM ⁻²

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Figure 141c List of Input Variables to the Systems Code

5. The performance of heat exchangers located in the laser gas flow loop is increased by operating the laser gas inlet temperature at elevated temperatures. This increase in efficiency is balanced by a decrease in intrinsic laser efficiency. For the input energies considered, the cooling requirements are minimal and 300°K was chosen as a nominal value.
6. The salt window cost is based upon Harshaw estimates of present costs.
7. Operating fluence levels for long lifetimes are unknown. The estimates of 1-2 J/cm² with time between refurbishing in excess of 10⁸ shots were obtained from LASL. There is little data to support this projection at the present time.
8. Present state of the art manufacture of salt disc is ~ 45 cm with 60 cm projected as near term goal.
9. The ratio of gain length to the diagonal of the PAM cavity of 0.7 is valid for low pulse repetition rates and cubic type PAM geometrics. A preliminary optimization to minimize the total number of PAM cavities for cubic type cavities results in the 70% figure. At high pulse repetition rates a different optimization is required.
10. EG&G thyatron have been tested up to voltages of ~ 200 kV.
11. Peak current ratings of thyratrons of 20 kA are presently available with near term projections of factor of 2-3 beyond this figure. Limitations in peak current appear to be associated with the manufacture of large ceramic tubes and not the basic cathodes themselves.
12. Thyratrons lifetime is primarily limited by the "on time" of the cathode heaters as opposed to the actual number of shots. Present tubes for radar applications have 500-1000 hr but specialized designs approach the one year limit.
13. Ignitrons have been designed for power plant service as high as the 100 kV level but general industrially rated versions are designed at ~ 25 kV.
14. Ignitrons have very high Coulomb transfer capabilities since they are basically high current, long pulse devices. Scaling to lower Coulomb transfer can significantly increase the lifetime of these switches.

15. Similar comments apply to the peak ignitron current.
- 16-18. High-voltage pulse transformers to couple a PFN to the discharge circuit have been used on AERL high repetition rate CO₂ lasers. Raytheon Corp., which has designed and built these units, considers the near term performance limits of pulse transformers to be 4 μ s(min) pulse duration at a 10:1 turns ratio at the megavolt level. The average power limitation is 1.67 MW and (quantity) product cost of \$10K is estimated.
- 19&20. Several capacitor manufacturers were surveyed to specify the performance characteristics of long life energy storage units. The maximum capacitor voltage, limited by internal corona is ~100 kV and the maximum energy storage at this voltage is ~300-500 J for long life, reliable operation. Minimum inductance of such units is limited to several tens of nanohenries and the cost per joule stored is ~\$0.40.
21. Power supply costs for large dc supplies is ~\$9/kW based upon AERL's experience on high repetition rate lasers.
22. There are numerous small electrical components including charging chokes, PFN inductors, various solid state devices for pulse shape control, etc. These costs do not significantly impact the total electrical system cost and are listed in the code for completeness.
23. The cost of concrete as a building material for laser flow tunnel and beam transport tubes has been a subject of some confusion. In the systems code, the cost of concrete has been referred to a volumetric cost for simplicity. In reality the concrete cost depends upon the materials costs can be specified per unit volume but other costs such as forms, site preparation, surface coatings, etc., are specified per unit area. Within the scope of this study, a simplified cost estimate is based upon volume has been assumed. An early estimate obtained from a local construction contractor was \$143/yd³ for flat concrete slabs poured in place. In addition to this charge, a cost of 82% was added on to cover costs of installation and assembly bringing the total concrete costs to \$260/yd³.

LASL has contracted the Architectural and Engineering firm of Burns and Roe to cost the reactor chamber building. Their concrete costs were significantly higher

due to several factors. First, nuclear containment buildings are constructed in accordance with rigid seismological and radiation safeguards. Secondly, the costs were heavily weighted by a square footage charge from forms and site preparation. To obtain a comparison with the earlier estimate, a typical thickness of 1/2 ft was assumed bringing the total cost to ~\$813/yds or a factor of ~300% higher than the previous estimate.

The laser flow tunnel and beam transport tubes need not be built to the rigid constraints of a nuclear containment building and if form charges dominate, significant cost reductions can be realized by standardizing and modularizing the design, e.g., identical tabular, interlocking sections to form a tunnel, either curved or straight. A local firm which specializes in prestressed concrete panels quotes \$3/ft² and \$12/ft² for standard or specialized panels respectively (including steel reinforcing and forms for ~1/2 ft thick panels). Translating these estimates into a volumetric charge, \$648/yd³ is obtained after the 82% assembly charge or \$356/yd³ for materials. The latter figure is used as the volumetric cost of concrete.

24. An alternative building material for large tunnel structures is steel and current estimates for simple plate still welded in place is \$1.60/lb.
25. Thermionic dispenser cathodes are projected at \$2/cm² exclusive of heater, mounting or radiation shielding structures.
26. The cost of fabricating diamond turned, large diameter optics is projected at \$15,000 per square meter and is assumed to scale with area.
27. Similar estimates for the mirror mount are \$20,000 per meter and are assumed to scale with diameter. These mirror and mount costs estimates were supplied by LASL.
28. Bushing cost estimates at the megavolt level are extrapolated from AERL's experience with bushings in the 200-300 kV range.
29. The number of bushings used per PAM is determined by inductance and load impedance considerations. The minimum number would be that required to subdivide the PAM cavity into a rectangular geometry and the maximum

would be that determined by bushing diameter and gain length. A general value of two bushings per PAM is used in place of more detailed estimates.

30. A building cost of \$10/ft² for noncritical buildings, e.g., metal prefabricated structures, which house equipment, supplies, etc., is assumed.
31. For long life operation of the e-beam diode section, oil-free pumps are desirable. Two types of pumps are generally used where oil contamination is a problem; turbomolecular and cryogenic pumps. AERL has extensive experience with turbomolecular pumps for e-beam applications and finds them satisfactory. At least one pump per e-beam section is required at a present cost of \$5900 each.
32. LASL has provided pellet gain data in the range specified. In the code, two values were chosen by specifying the higher pellet gain (for a given laser energy) and multiplying by 1 (optimistic) or 1/2 (pessimistic).
33. Target energies of 1-5 MJ are of interests and 3 values were chosen for the code runs.
34. The pulse repetition rate ranges from 2 Hz to 50 Hz.
35. The aerowindow breakdown flux is a function of pulse duration as shown in Figure 102. In the code, a polynomial curve is fitted to the data permitting an analytical expression for the number of pulses (series and parallel combination of Figures 104 and 105) which can be passed through a single aerowindow.
36. An average value of 15 MW/M² is chosen as the electrical power required to overcome flow losses in the aerowindow.

C. KINETICS CODE ALGORITHMS

1,2. The inputs and outputs of the AERL kinetics code are summarized in Figure 142 and displayed in detailed in Tables I-III of Section III. For each value of gas pressure and N₂:CO₂ mixture ratio, the optimum E/P is found. For each of the four values of electron density, the input energy density, small signal gain and time to reach the operating pumping point (90% of peak efficiency) are calculated. The energy density stored in the population inversion is obtained from the product of input energy density and upper state pumping efficiency. The discharge current density is recorded and the gas resistivity is found by dividing the discharge electric field by the current density.

B. CODE ALGORITHMS

1. INPUT TO AERL KINETICS CODE

- PRESSURE
- ELECTRON DENSITY
- GAS MIXTURE

2. OUTPUT OF KINETICS CODE

- OPTIMUM E/P
- INPUT ENERGY DENSITY
- OUTPUT ENERGY DENSITY
- SMALL SIGNAL GAIN
- TIME TO REACH OPTIMUM PUMPING
- DISCHARGE CURRENT DENSITY
- GAS RESISTIVITY

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Figure 142 Kinetics Code Algorithms

D. CAVITY DESIGN ALGORITHMS

The relations used to determine PAM cavity design are listed in Figure 143. The gain length, L_C , is determined in two different ways depending upon the pulse repetition frequency. In the low frequency case where cubic type PAM cavities result, the gain length is chosen as a fraction of the maximum product of small signal gain and length which is allowed by parasitic considerations. Here the maximum length in the cavity is the diagonal measurement given as the square root of the sum of the squares of the 3 PAM dimensions. In the low frequency case, a separate optimization for cubic or near cubic cavities shows that the fraction which results in the minimum number of PAMS is

$$g_0 L_C / g_0 L \cong 70\%$$

where $(g_0 L)_{MAX} \cong 10$. In the high frequency case, $L_C \cong L \cong 10/g_0$ since the PAM cavities are much longer than they are wide.

The PAM cavity dimension in the discharge dimension is fixed by the maximum system voltage and the optimized electric field. The maximum system voltage is largely a function of the state-of-the-art technology in high-voltage components, i.e., bushings, switches, capacities, etc. Presently, 1 MV is feasible and near term developments should increase this working voltage to 2 MV.

In the flow dimension, the PAM cavity dimension is determined by either parasitics or flow considerations for low or high pulse repetition rates respectively. In the low frequency case, the values of L and L_C are fixed by parasitics and the 70% optimization relationship. The value of W is then determined by the diagonal constraint,

$$W = \sqrt{L^2 - L_C^2 - H^2}$$

where H is fixed by voltage considerations. The flow Mach number relationship is then used to calculate the laser gas Mach number up to the point $M = 0.25$. At pulse repetition frequencies for which $M > 0.25$, a high frequency assumption is used. The break-point for changing the model at $M = 0.25$ stems from the observation that although the pulse repetition frequency is an increasing function of Mach number for $0 < M < 0.25$, the rate of change slows above $M > 0.25$. Further, the electrical power required to power the laser gas flow loop varies as M^3 so that the ratio of flow power to laser output power varies as M^2 .

3. CAVITY DESIGN

– APERTURE LIMIT

$$E_{so} P_c + 0.67 g_o L_c E_{so} P_c N_{eff}$$

– PARASITIC LIMIT

$$g_o L_c / g_o L$$

– MULTIPASS EFFECTIVENESS

$$N_{eff} = \sum_{j=0}^{N_p-1} [1 - 2/3 (1 - \psi_N)]^j$$

– DISCHARGE DIMENSION

$$H E = V_{DISCH}$$

– FLOW DIMENSION

$$(PRF) 2 W/a = m (1 - m)$$

$$L^2 = L_c^2 + H^2 + W^2$$

– NUMBER OF PAMS

$$HWE_s (1 + 2/3 N_{eff} g_o L_c) N_L = E_L$$

J1543

Figure 143 Cavity Design Algorithms

Above the $M = 0.25$ breakpoint, the high frequency model is used. The PAM cavity is much longer than it is wide and the assumption, $L = L_C$, is made. In this case, the flow Mach number is fixed at 0.25 and the flow relation is used to calculate W for a fixed pulse repetition frequency.

There is still one situation not covered by these two cases of low and high pulse repetition frequency. For conditions of low frequency and high small signal gain, the Mach number can be still below the 0.25 breakpoint and the code will terminate. The difficulty here is that for low pulse repetition rates, the diagonal relationship is still used and for large values of the sustainer voltage, the value of W can drop to zero. At this point the code terminates. The code could be structured to switch to the high pulse repetition rate model with this termination situation but, in general, the Mach number would jump discontinuously. Another alternative would be to lower the value of sustainer voltage which then reduces the dimension, H , and yields a positive value for W . This course would be more logical from an actual design standpoint. However, the number of situations for which this termination occurs is low and the accompanying values of small signal gain and electrical pulse width are inappropriate for a feasible overall system design. Therefore, the code has been left to terminate under these conditions.

In all cases, the aperture fluence is calculated assuming a saturated input on the first pulse, i.e.,

$$E_{IN} = E_{SO} P_C$$

where P_C is the cavity pressure. The aperture fluence on the final pulse is calculated from the analytical formulation of N_{eff} which relates the ratio of the total multipulse to first pulse energy. Checks are made throughout the code to insure that the aperture fluence does not exceed the mirror, air breakdown or salt window limits. The total number of PAM (excluding those required for preamplifiers) is determined from the total output required, the aperture fluence and aperture area.

The total energy which must be supplied by the preamplifiers is calculated from the total PAM aperture area and the requirement that the input pulse be of the saturation fluence. It is assumed that the preamplifier design is identical to the PAM and that a mirror system splits up the preamplifier beams into the correct number and size for PAM input.

E. LASER GAS FLOW PROPERTIES

The flow properties of the laser gas system are summarized in Figure 144. The mass flow rate per PAM is given by the product of density, flow velocity in the cavity, and area perpendicular to the flow. The dimension parallel to the discharge is increased by a factor to account for space behind the electrodes. In the dimensions parallel to the laser axis, additional space is allotted to prevent high voltage breakdown from the anode to the cavity box.

The makeup rate for each PAM is taken at 0.1% of the flow rate. The loss results from leakage of the shear layers in the aerocurtains which contain the laser gas within the PAM cavity.

The electrical power necessary to drive the laser gas flow system is expressed as a fraction of the circulating power referenced to the flow conditions. The electrical power is related to the pressure drops at each component of the flow system, i.e., heat exchangers, diffusers, screens and compressors. The local pressure drops are referenced to the PAM cavity by the square of the area contraction rates which is related to the physical size (and, therefore, cost) of the laser gas flow tunnel.

F. AEROWINDOW FLUENCE

A series-parallel stacking arrangement is used to multiplex several PAM output beams through a single aerowindow. The PAM output beams are decollimated to the fluence limit of the mirrors and then stacked in parallel (at shallow angles) to the fluence limit set by air breakdown in the aerowindow aperture. The optical schematic is outlined in Figures 104 and 105 of Section VI. The parallel bundles are then staggered in time by a pulse width and passed through the aerowindow aperture as a long, composite, pulse with no dead time. A curve fit to the air breakdown curve, $B(t)$ allows a self consistent solution for the number of series pulses per aerowindow. In this formulation "t" is the total pulse width, i.e., number of series pulses multiplied by the individual pulse width. The equations are outlined in Figure 144.

G. AEROWINDOW DESIGN

A subsonic aerowindow design has been chosen for the code. The full equations for the performance of this device is found in the report. It is assumed that the aerowindow provides a pressure interface between the laser cavity at pressure p_c and the beam transport tube at 0.01 atm. The low pressure aerowindow which shields the final optic from the soft x-ray flux will provide the final pressure interface between the beam transport tube and the

4. FLOW PROPERTIES

- MASS FLOW RATE

$$\text{Ma} (L_c + H) (H + d) (\text{DENSITY})$$

- MAKE-UP RATE

$$10^{-3} \text{ FLOW RATE}$$

- FLOW POWER

$$\frac{1}{2} (\text{Ma}) (\text{MASS FLOW RATE}) (\text{LOSS FACTOR})$$

5. AERO WINDOW FLUENCE

- AERO REDUCTION TO MIRROR LIMIT

$$(\text{HW}) E_{so} P (1 + 2/3 N_{eff} g_o L_c) (\text{MIRROR LIMIT})^{-1}$$

- SERIES/PARALLEL BEAM STACKING

$$N_{SER} N_{PAR} (\text{MIRROR LIMIT}) = N_{SER} T_P B (N_{SER} T_P) N_{WIND.}$$

6. AERO WINDOW DESIGN, SUBSONIC MULTISTAGE

- NUMBER OF STAGES

$$2.46 \cdot \text{LN} (P_c / .01)$$

- A/W LENGTH

$$(\text{NUMBER OF STAGES}) (\text{AREA/ASPECT RATIO})^{1/2}$$

- MASS FLOW

$$N_{AW} (P_c MW_t / T) (\text{Ma}) (\text{AREA}) [1 - (2/3) N_{ST}]^3$$

- POWER CONSUMED

$$(\text{MASS FLOW}) RT \text{LN} (\Delta P_{co}) (.01515)$$

J1547

Figure 144 Properties of the Laser Gas Flow System

target chamber. The number of stages, assuming a pressure differential per stage of 1.5 is proportional to the logarithm of the pressure ratio. The total length of the aerowindow follows from the number of stages, the aspect ratio of the window and the aerowindow aperture. In this model the aperture is the PAM aperture reduced by the ratio of PAM output fluence to mirror limit fluence. The total mass flow and power consumption follow from the equations presented in the report.

H. LOW PRESSURE AEROWINDOW

The design of the low pressure aerowindow is identical to that of the high-pressure device with the exception that no beam multiplexing is involved and one stage is used.

I. PREAMPLIFIERS

The design of the preamplifiers is identical to that of the PAM. The output energy requirements for the preamplifiers are fixed by the total aperture of the PAMS and the requirement that the PAM input be at least the saturation fluence. An optical system separates the preamplifier output into the required number of beams and enlarges their cross section to match the PAM.

J. DISCHARGE POWER SUPPLY

The equations describing the two PFN options for the discharge power supply are outlined in Figure 145. For pumping durations in excess of 4 μ s, an indirectly coupled PFN is used with a pulse transformer between the PFN and the discharge load. This arrangement permits the use of thyatron switchgear and state-of-the-art PFN Design concepts and components. For pulse durations less than 4 μ s transformer coupled PFN designs do not appear feasible at present or in the near term and, hence, a direct coupled design is required. As was reported in the text, the only switch which is presently capable of high-voltage current operation in a stacked configuration is spark gap, however, the lifetime estimates of such a switch are inadequate. In the interests of comparing long and short pumping pulse durations, it was decided to price in an ignitron switch which may have adequate lifetime characteristics if properly derated. It is assumed that these switches can be stacked to the proper voltage and, hence, only the component costs are considered. In this way, the ignitron assumption represents a risk-benefit analysis in the following sense: if ignitrons (or spark gaps) can be engineered to give adequate performance (or lifetime) so that only the component cost is considered in pricing, the code will estimate the benefits to this development in the cost savings which accrue for the total system.

7. LOW PRESSURE AERO WINDOW

- NO MULTIPLEXING, ONE STAGE SUBSONIC
- SAME DESIGN AS HIGH PRESSURE WINDOW

8. PREAMPLIFIERS

- SAME DESIGN AS PAM
- OUTPUT ENERGY

$$E_{so} P_c HW N (PAMS)$$

9. DISCHARGE POWER SUPPLY

- $T_p > 4$ MICROSEC, TRANS. COUPLED-THYRATONS
- PEAK CURRENT

$$e N_e V_d (E/P) L_c W \text{ PER PAM}$$

- LOAD RESISTANCE

$$V_{DISC} / I_{DISC} \text{ PER PAM}$$

- INPUT ENERGY

$$V_{DISC} I_{DISC} T_p \text{ PER PAM}$$

- COULOMB TRANSFER

$$10 I_{DISC} T_p \text{ PER PAM}$$

- SWITCH CURRENT (AT XFMR PRIMARY)

$$I_{PK} = 10 I_{DISC} \text{ PER PAM}$$

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$$I_{AVG} = I_{PK} T_p \text{ PRF}$$

Figure 145a Discharge Power Supply Algorithms

- SWITCH VOLTAGE (AT XFMR PRIMARY)

$$V_{PK} = 0.2 V_{DISC}$$

- NUMBER OF THYRATRONS

$$I_{PK} (20 \text{ kA})^{-1} I_{AV} (10 \text{ A})^{-1} V_{PK} (200 \text{ kV})^{-1}$$

- NUMBER OF TRANSFORMERS

$$I_{DISC} V_{DISC} T_P N (\text{PAMS}) \text{ PRF } (1.67 \text{ MW})^{-1}$$

- STORED JOULES PER PULSE

$$I_{DISC} V_{DISC} T_P N (\text{PAMS}) \text{ AT } \$40 (\text{JOULE})^{-1}$$

NO PRF DEPENDENCE BELOW 15 Hz

- MISCELLANEOUS COMPONENTS

CHARGING DIODES

END OF LINE CLIPPERS

RESISTORS

CHOKES

- D.C. POWER SUPPLY

$$I_{DISC} V_{DISC} T_P N (\text{PAMS}) \text{ PRF AT } \$9 (\text{kW})^{-1}$$

- $T_P < 4 \mu\text{SEC}$ - DIRECT COUPLED, IGNITRONS

J1556

Figure 145b Discharge Power Supply Algorithms

- IGNITRON LIFE TIME

$$10^3 (300 \text{ kA}/I_{PK})^3 (200 \text{ C}/I_{PK} T_P)^2$$

- NUMBER OF IGNITRONS – CHOOSE SMALLER

$$(2 V_{DISC}/25 \text{ kV}) (I_{DISC} N_{PAMS}/I_{IG})$$

OR

$$(2 V_{DISC}/25 \text{ kV}) (S) (N_{STAGES}) N_{PAMS}$$

10. FLOW TUNNEL, MODEST DESIGN FOR LOW TO MODERATE PRF SEVERAL PAMS FOR COMPRESSOR

- LASER GAS TEMPERATURE RISE

$$(\text{DENSITY}) C_P \Delta T_{CAV} = Q_{INPUT} (1 - \text{EFF})$$

- HEAT EXCHANGER EFFICIENCY, REFERENCED TO LASER CAVITY

$$\Delta P/P_c = (\gamma M_c^2/2C^2) [2.76 + 7.18 \text{ LN} (1 + \Delta T_{CAV}/\Delta T_{COOL})]$$

OR

$$\Delta P/P_c = (\gamma M_c^2/2C^2) (1 - C^{-2}) \text{ CHOOSE LARGER}$$

- FLOW MODULES PER COMPRESSOR

$$10\% (\text{FLOW MODULE PRESSURE DROP})^{-1}$$

J1544

Figure 145c Discharge Power Supply Algorithms

Thyratrons are assumed to operate at 200 kV so that no voltage stacking is needed for 1 MV discharge voltage with a 10:1 step-up pulse transfer. The number of thyratrons needed is determined by the peak current limitations on the tube since the average and RMS currents are low in this system. The input energy to the PAM is given by the total voltage, current and pulse duration which are outputs from the kinetics code. These voltages and currents are reflected to the primary side of the transformer and the number of tubes is calculated. The number of transformers is fixed by average power limitations with each transformer capable of 1.67 MW of average power. The cost of the total energy stored per pulse is priced at \$0.40/J, an estimate for long life and moderate pulse repetition frequency. Miscellaneous components such as charging drodes, end of line dippers, resistors, chokes, etc., are also priced in and the dc power supply is priced at \$9.00/kW for large supplies.

For pulse durations less than 4 μ s, the PFN is directly coupled to the discharge load and high-voltage switchgear is needed. Ignitrons are chosen for their long life and high current capability. Questions as to voltage stacking inductance and pulse repetition frequency are unresolved at this time. The lifetime of ignitrons is assumed to vary inversely as the cube of peak current and inversely as the square of coulomb transfer. Once the current is specified commensurate with the lifetime requirement (1 yr at 10 Hz), the number of ignitrons is fixed by either total current or PFN geometry which is smaller. The cost of the other PFN and power supply components are costed in the same manner as the transformer coupled PFN.

K. LASER GAS FLOW TUNNEL

The general design for the laser gas flow tunnel is shown in Figures 130 and 133 of Section VII and the algorithms are outlined in Figures 145 and 146. All the PAMS, including those required for preamplifiers are stacked in series in a large torus. At regular intervals, specified by maximum allowable pressure drop across a PAM, fan-compressor units are placed to maintain the flow Mach number. A detailed design of the flow system optimizing the power consumption has not been carried out for the code because at the low to moderate pulse repetition rates relevant for the Laser Fusion Driver, the ratio of power consumption to input power to the laser is not large and, hence, overall efficiency is not critical factor.

The flow system layout consists of a wide angle diffuser just downstream of the PAM cavity at the end of which is a heat exchanger. The back pressure generated by the heat exchanger reduces the boundary layer separation problem in the diffuser and allows opening angles approaching 40° to be used. The cooled flow is then passed through screens to reduce the turbulence

- COMPRESSOR DESIGN

$$d = P (L_c + H, CH)^2 / (\text{MATERIAL STRENGTH})$$

$$\text{VOLUME} = 2 (CH + L_c + H) (3.3 CH) d$$

- FLOW MODULE DESIGN

$$\text{VOLUME} = 2 (CH + L_c + H) (4CH + 2W + .5H) d$$

- COMPRESSOR VOLUME FLOW RATE

$$M_c R (T + T_{CAV}) / P_c \text{ AT } \$2 / \text{ACFM}$$

11. GAS RECOVERY – MAKE-UP SYSTEM

- 10^{-3} MASS FLOW
- CO_2 REMOVED BY CHEMICAL ABSORPTION
- N_2 , CO_2 GENERATED, MIXTURE CORRECTED
- PLANT COSTS – SUBROUTINE

12. COOLING SYSTEM

- LOW PRESSURE FOR PAMS

$$(\text{INPUT ENERGY}) (\text{PRF}) / (\Delta T C_W)$$

J1551

Figure 146 Laser Gas Flow Tunnel Algorithms

scale, accelerated in a contraction section and passed through the next PAM cavity. At regular intervals, a fan-compressor unit is inserted to maintain the flow.

The primary scaling parameter is the contraction ratio. Large values of the contraction ratio reduce the pressure drops of the heat exchanger and screens (when referenced back to the PAM cavity section), thereby reducing the number of fan-compressor units needed for the entire system. However, large values of the contraction ratio lead to large diameter flow tunnels and longer lengths for each of the expanded sections of the tunnel. The contraction ratio is left as a parameter with a nominal value of 3.

The performance of the heat exchanger is calculated from the temperature rise of the gas and the cooling water temperature. The temperature rise of laser gas is calculated from the input power and heat capacity of the gas and the local pressure drop is referenced to the PAM cavity using the flow Mach number and contraction ratio. If the temperature use of the gas is sufficiently low so that the local pressure drop of the heat exchanger becomes less than the pressure drop from the diffuser section, then the diffuser pressure drop is used.

The pressure drops of the heat exchanger, diffuser and screens are calculated referenced to the PAM cavity conditions. When the total pressure drop exceeds a predetermined value, arbitrarily chosen as 10% of the cavity pressure, a fan-compressor is inserted in the flow tunnel to make up the pressure drop. The 10% figure is reasonable since large fan-compressors can supply pressure differentials of 10-20%.

The materials cost for the flow tunnel is calculated from a volumetric standpoint. The wall thickness of the structure is estimated from the pressure differential, maximum unsupported span and the strength of materials, either reinforced concrete or steel. The cross section of the flow tunnel is assumed to be constant and equal to the fully expanded value. Specific contours are made with thin sheet stock within the steel or concrete pressure vessel. The contraction ratio specifies both the perimeter and length of the tunnel (see Figure 130 of Section VII) and this information together with the wall thickness specified the total material volume. The cost of the tunnel is then estimated from the volumetric materials cost, either \$356/yd³ for concrete or \$1.60/lb for steel, plus an installation factor of 82% of the material cost. The compressor cost is given by the required flow rate times a cost of \$2/std ft³/min, a figure which is representative of large (air-craft type) fan-compressor units.

L. GAS RECOVERY AND MAKEUP SYSTEM

The makeup rate for the laser gas flow system is taken at 0.1% of the main laser gas flow rate. The gas flow for the aerowindows and the beam transport tubes is nitrogen so that only N_2 and CO_2 are present in the system. A portion of the laser gas leaks out of the aerocurtains and is collected. The CO_2 is removed by chemical absorption leaving only N_2 . Extra CO_2 is generated by burning carbon. Nitrogen from the aerowindow supply is then mixed with the CO_2 and fed back into the laser mix to maintain the gas supply.

M. COOLING SYSTEM

Two cooling systems are provided, a low pressure water system for the laser gas flow heat exchangers and a high pressure system to cool the optical elements. The relations are outlined Figures 146 and 147. The cooling water requirements for the laser gas heat exchangers are estimated from the input power to the gas. To first order, the laser output energy can be ignored in this calculation.

To estimate the cooling requirements for the high pressure system the average flux incident on each of the mirrors is calculated and a 1% absorption coefficient is assumed. The pump drives for each system is costed as is the piping necessary to get cooling water to the PAMS. In the piping estimate it is assumed that the source of cooling water is located in a torus diameter from the Laser Fusion Driver.

N. BEAM MULTIPLEX TUBES

The relations describing the beam multiplex tubes are summarized in Figure 147. The diameter of the tubes depends upon the number of multiple passes. At the end of the beam tubes, the beams are fully separated and arranged in a pattern which gives the smallest diameter tube. It is assumed that the beam tube is held at full diameter when pricing the materials. The tube length is determined by the larger of two criteria. To have less than 10% of the PAM active volume wasted by the shallow angle passage of the beams through the PAM, the multiplex tube length must be at least 10 times the PAM gain length. However, the transit time between passes through the PAM must be at least twice the vibrational repump time which expressed in feet is $30 \text{ m } P_c^{-1}$ where P_c is the cavity pressure (in atmospheres). For cavity pressures up to ~ 2 atm and multiple passes of 5 or less the limiting criterion is the vibrational repump time.

The number of multiplex tubes is twice the number of PAMS and their volume is estimated in a manner similar to that developed

- HIGH PRESSURE FOR OPTICS

$$\frac{(\text{LASER OUTPUT ENERGY}) (\text{PRF}) (\text{TOTAL MIRROR ABSORPTION})}{(\Delta T C_W)}$$

- HIGH AND LOW PRESSURE PUMPS PLUS DRIVES
- PIPING AT TORUS DIAMETER

13. BEAM MULTIPLEX TUBES

- DIAMETER AND LENGTH

$$\begin{aligned} 1 \text{ PASS, } D &= 1.2 (H^2 + W^2)^{1/2} \\ L &= L_c \end{aligned}$$

$$\begin{aligned} 2 \text{ PASS, } D &= 1.2 (4H^2 + W^2)^{1/2} \\ L &= 30 \text{ METERS} / P_c \end{aligned}$$

$$\begin{aligned} 3,4 \text{ PASS, } D &= 1.2 (4H^2 + 4W^2)^{1/2} \\ L &= 30 \text{ METERS} / P_c \end{aligned}$$

$$\begin{aligned} 5 \text{ PASS, } D &= 1.2 (9H^2 + 4W^2)^{1/2} \\ L &= 30 \text{ METERS} / P_c \end{aligned}$$

- NUMBER OF TUBES - 2N (PAMS)
- VOLUME OF TUBES - SAME FORMULAE AS FLOW TUNNEL

J1554

Figure 147 Cooling System Algorithms

for the laser flow tunnel. The wall thickness must support any pressure differential and the surface area is fixed by the maximum diameter and length.

O. TRANSFER TUBES

The transfer tubes take the final optical pulse from the multiplex set and transfer it to the pressure interface, either the aerowindow or the salt window. At the end of the multiplex passes, the beam aperture is equal to that of the PAM, however, its fluence is approximately a factor of two lower than the mirror damage limit. To conserve materials the beam is reduced in area to the mirror limit. Thus the transfer tube aperture is equal to the PAM reduced by the ratio of output fluence to mirror damage fluence. The length of the tube is approximated by the radius of the torus and the materials cost is calculated in the same manner as the other structural components.

P. SALT WINDOW

The salt window configuration chosen for the code is the mosaic depicted in Figure 115 of Section VI and outlined in Figure 148. The output of the PAM is expanded to the fluence limit of the salt discs, either 1 or 2 J/cm², and passed through the mosaic. Individual optic elements recollimate the beamlets and place them onto one mirror at the mirror fluence limit. In this scheme, no provision is made to actively control the phase of the beamlets so that phase coherency among the beams is lost if quantities change in time. It is assumed that the optics can be initially adjusted to give a fully coherent beam on the lost mirror. The number of salt discs is given by the laser output, the salt fluence limit and the maximum salt diameter. At present, 45 cm is the maximum diameter with 60 cm projected in the near future. The mosaic frame is priced at 65 K/PAM.

It would be possible to multiplex several PAM output beams through a single salt window mosaic if the beams were suitably staggered in time. However, each PAM beam still requires a recollimating array and, thus substantial cost reductions may not be possible with this multiplexing. In the code, it is assumed that there is a separate salt window mosaic for each PAM beam.

Q. OPTICS - AEROWINDOW OPTION

The algorithms for A/W optics are outlined in Figure 149. The cost per mirror is taken as \$15 K for a one-meter diameter mirror (diamond turned) and is assumed to scale as the area. The cost of the mirror mount is taken as \$20 K for a one-meter diameter mirror and is assumed to scale as the diameter. The number of

14. TRANSFER TUBES

- DIAMETER

$$(HW) (APERTURE \text{ FLUENCE}) (MIRROR \text{ FLUENCE})^{-1}$$

- LENGTH

$$RADIUS \text{ OF TORUS}$$

- TUBE VOLUME - SAME FORMULAE AS FLOW TUNNEL

15. SALT WINDOW OPTION, EXPAND BEAM TO SALT FLUENCE LIMIT,
PASS THROUGH MOSAIC RECOLLIMATE DOWN TO MIRROR LIMIT.

- NUMBER OF SALTS

$$(LASER \text{ OUTPUT}) (\pi D^2/4)^{-1} (SALT \text{ FLUENCE})^{-1}$$

- MOSAIC FRAME, 1 PER PAM AT 65K

J1555

Figure 148 Salt Window Algorithms

16. OPTICS, AERO WINDOW OPTION

- MULTIPLEX MIRRORS
2N (PASSES) N (PAMS)
- COST PER MIRROR
 $(\text{AREA}) (\$15\text{K}/\text{M}^2) + (\text{AREA})^{1/2} (\$20\text{K}/\text{M})$
 $\text{AREA} = (2)^{1/2} \text{HW}(\text{APERTURE FLUENCE}) (\text{MIRROR FLUENCE})^{-1}$
- TRANSFER MIRRORS
5 N (PAMS)
- COST, SAME AS MPX MIRRORS

17. OPTICS, SALT WINDOW OPTION

- RECOLLIMATING OPTICS = NUMBER OF SALTS
- COST, SAME AS MPX MIRRORS BUT
 $\text{AREA} = \pi D_{\text{SALT}}^2 / 4$

18. ROTATING MIRROR, 4 FACES AT 10 Hz

- NUMBER, 1 PER PAM
- COST, SAME AS MAX MIRRORS PLUS \$60K FOR MOUNT

J1548

Figure 149 Aerowindow Optics Algorithms

multiplex mirrors is twice the number of passes for each PAM. The area of the multiplex is equal to the PAM aperture times the square root of two to account for the 45° angle of incidence.

The transfer mirrors take the output beam from the multiplexed PAM and transfer it to the aero or salt window. The specific design of the optical train is not fixed for the code and a reasonable estimate of 5 transfer mirrors per PAM is assumed. To conserve space, the output beam is reduced in area to the fluence limit of the mirror. Hence, the transfer mirror area is equal to the PAM aperture reduced by the ratio of aperture fluence to mirror damage fluence times the square root of two for tuning. The costs of the transfer mirrors is computed in a similar manner for the multiplex mirrors.

R. OPTICS - SALT WINDOW OPTION

An additional optics cost is required for the salt window option considered, that being the recollimating optics located behind the salt mosaic. The number of these optics is equal to the number of salt discs in the mosaic and their area is identical to the area of the salt discs.

S. ROTATING MIRROR

In the baseline reactor configuration specified by LASL, four reactor chambers are used each at a repetition rate of a few Hz with the Laser Fusion Driver beam switched sequentially between the four chambers. A 4 faceted, rotating mirror, at 10 Hz is costed assuming one such optic per PAM. The area of each face is the PAM aperture reduced by the ratio of aperture fluence to mirror damage fluence. The cost of the mirrors is calculated as before with \$60 K per mount allotted as well.

T. ECONOMIC DATA

The economic data outlined in Figure 150 was used to estimate the cost of electric power (mils/KwHr) and was supplied by LASL and pertains only to the Laser Fusion Driver. All costs pertaining to the reactor chamber or grid system are excluded from this estimate. The total electric power output is a product of laser energy on target, pellet gain, thermal to electrical conversion efficiency of 30%, and the plant duty cycle. The net electric power is this total amount minus the total electric power required to energize the Laser Fusion Driver. Capital amortization is assumed to be 15% for the cost of money. The direct amortization is then the total capital cost times the 15% per year cost of money spread over the entire net electric power output for the year, expressed in mills per kilowatt hour.

19. ECONOMIC DATA

- TOTAL PLANT OUTPUT

$(E_T G_{\text{PELLET}} \text{ PRF}) \text{ 30\% DUTY FACTOR} - E_T \text{ EFF}^{-1} \text{ PRF}$

- CAPITAL AMORTIZATION - 15%

- INDIRECT COST AMORTIZATION

CONSTRUCTION FACILITIES AND EQUIPMENT
ENGINEERING AND MANAGEMENT INTEREST
DURING CONSTRUCTION

- LABOR AND MAINTENANCE

FIXED COST OF LABOR AND MAINTENANCE
COST OF LASER POWER SUPPLY MAINTENANCE
COST OF REACTOR CAVITY MAINTENANCE

J1550

Figure 150 Algorithms for Economic Data

The indirect cost is calculated by taking a fraction of the total capital cost for construction facilities and equipment and engineering the management costs incurred during construction. The ratio of indirect to direct costs is taken from recent nuclear power plant projects. A similar cost is computed for labor and maintenance. This cost includes a fixed amount for Fusion Driver labor and maintenance plus an estimate of yearly power supply and reactor cavity maintenance. These costs are also expressed in mils per kilowatt hour and are added to the capital cost for a total figure.

U. COMPUTER TEST RUNS

The Laser Fusion Driver Systems code has been run for a wide range of parameters and in fact led to the choice of the baseline design presented in Section I. The subatmospheric case was the only case in which the electrical pulse duration was sufficiently long to permit a pulsed power system which is realizable by present or near term technology. The specific case chosen for baseline design was also the least expensive (in capital costs) out of all the 1/2 atm runs primarily due to the low value of electron density and high intrinsic laser efficiency. In this section, a range of pulse repetition rates are run for the baseline case to see the effect of high average powers on the system costs (see Table VI). The 1 MJ case is representative of typical system costs since the PAMS are already modularized to their fullest extent and therefore an increase in pulse energy will increase system costs in approximately the same ratio.

The following is a list of system parameters which have been called out of the code.

1. V_{sus} = sustainer voltage (MV)
2. PRF = pulse repetition frequency (Hz)
3. P_c = cavity pressure (atm)
4. mix = N_2 to CO_2 ratio
5. n_e = electron density ($\text{cm}^{-3} \times 10^{12}$)
6. τ_{pe} = electrical pulse width (μs)
7. H = PAM cavity dimension parallel to e-beam (m)
8. L = PAM cavity dimension parallel to optical axis (m)
9. W = PAM cavity dimension parallel to flow axis (m)

TABLE VI

RUN	1 V _{SUS} mv	2 PRF Hz	3 P _C atm	4 MIX N ₂ :CO ₂	5 n _{e3} cm ⁻³	6 τ _{pe} μs	7 H m'	8 L m'	9 W m'	10 N _{PAM} -	11 CES+ CIES M\$	12 CEB+ CIEB M\$	13 CMU+ CIMU M\$	14 CCCS+ CICCS M\$	15 CRT M\$	16 CRT M\$	17 CTT M\$	
1	1.0	2	1/2	1:1	2x10 ¹²	4.1	1.6	3.3	3.0	15	29	14	1.6	0.1	9	52	6	
2	1.0	10	1/2	1:1	2x10 ¹²	4.1	1.6	3.3	2.9	15	31	14	10	0.9	37	50	7	
3	1.0	20	1/2	1:1	2x10 ¹²	4.1	1.6	3.3	1.4	30	35	14	20	1.8	73	61	14	
4	1.0	40	1/2	1:1	2x10 ¹²	4.1	1.6	3.3	0.7	60	43	15	40	3.4	146	77	41	
RUN	18 CRM M\$	19 CRT M\$	20 CROTM M\$	21 CAWI M\$	22 CAM M\$	23 CSALT M\$	24 CAMS M\$	25 η _{eL} %	26 η _{LFDA} %	27 η _{LFDS} %	28 CSTOTA + CITALA M\$	29 CSTOTS CITALS M\$	30 LRT m	31 RLOOP m	32 DTR m	33 DLT m	34 CDPWTA \$/W	35 CDPWTS \$/W
1	27	3	2	3	1	22	4	11.5	6.0	8.1	229	268	60	96	9	7.2	115	134
2	27	3	2	3	1	22	4	12.0	7.5	8.0	263	303	60	122	9	7.2	26	30
3	30	4	3	1	1	23	4	12.0	7.9	8.0	356	398	60	435	5	7.2	14	15
4	35	5	5	1	1	25	4	12.2	8.1	8.2	550	596	60	435	5	7.2	14	15

10. N_{PAM} = total number of PAMS including preamplifiers
11. CES+CIES = cost of electrical driver for the sustainer discharge including installation ($\$ \times 10^6$)
12. CEB+CIEB = cost of e-beam system including installation ($\$ \times 10^6$)
13. CMU+CIMU = cost of the gas makeup system including installation ($\$ \times 10^6$)
14. CCS+CICCS = cost of the water cooling system ($\$ \times 10^6$)
15. CLT = cost of the laser flow tunnel including installation-concrete option at \$143/cu yd ($\$ \times 10^6$)
16. CRT = cost of multiplex tubes ($\$ \times 10^6$)
17. CTT = cost of transfer tubes ($\$ \times 10^6$)
18. CRM = cost of relay mirrors ($\$ \times 10^6$)
19. CTM = cost of transfer mirrors ($\$ \times 10^6$)
20. $CROT_M$ = cost of the rotating mirror ($\$ \times 10^6$)
21. CAWI = cost of the main aerowindow ($\$ \times 10^6$)
22. CAM = cost of the optics for the main aerowindow ($\$ \times 10^6$)
23. CSALT = cost of salt windows including window frame ($\$ \times 10^6$)
24. CAMS = cost of mirrors for salt window array ($\$ \times 10^6$)
25. η_{eL} = intrinsic laser efficiency equal to the ratio of laser energy out to energy in the sustainer discharge (%)
26. η_{LFDS} = total laser fusion driver efficiency for aerowindow option (%)
27. η_{LFDS} = total laser fusion driver efficiency for salt window option (%)
28. CSTOTA+CITALA = total system capital cost for aerowindow option ($\$ \times 10^6$)

- 29. CSTOTS+ CITALS = total system capital cost for salt window option (\$ x 10⁶)
- 30. LRT = length of the relay tubes (m)
- 31. RLOOP = diameter of the (toroidal) laser flow tunnel (m)
- 32. DTR = diameter of the relay tubes (m)
- 33. DLT = diameter of the flow tunnel (m)
- 34. CDPWTA = cost of laser power in capital dollars per watt - A/W option
- 35. CDPWTS = cost of laser power in capital dollars per watt - S/W option

The cost of concrete is estimated at \$356/yd³. The estimates for the salt window option are based upon a 1 J/cm² working fluence, 1-year lifetime and a diameter of 45 cm. Further, all dollar estimates are rounded up to the nearest million with the exception of the cooling system. The cost of the system rises slower than the repetition rate and hence the capital cost per watt of laser power produced drops with increasing repetition rate.

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