MASTERCONCEPTUAL DESIGN
OF A KrF SCALING MODULE

FINAL REPORT

Contract DE-AC08-79DP40115

OCTOBER 1980

To
DEPARTMENT OF ENERGY
Nevada Operations Office
PO Box 14100, Las Vegas, NV 89114

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ABSTRACT

A conceptual design of an angular multiplexed 50 kJ KrF laser module for Inertial Confinement Fusion is presented. Optical designs for encoding, beam packing and beam transfer between amplifier stages are developed; emphasis is placed on reducing prepulse problems and achieving acceptable optical quality. An axisymmetric optical design is identified as optimum in terms of simplicity, optical quality, cost and alignment. A kinetic code model was developed for the KrF amplifier and was used to derive scaling maps for the 50 kJ module. Attention was given to reducing parasitics, achieving acceptable extraction efficiency and accounting for amplified spontaneous emission effects. The size of the module is constrained by parasitic suppression and damage thresholds; the power gain is constrained by demanding 40% extraction efficiency in a double pass extraction geometry; and, the run time is constrained by the pulsed power technology (PFN or PFL) and acceptable values of $g_0 L$. The bounds imposed on the design by the pulsed power technology were examined. Both PFLs and PFNs were considered along with their associated diode, hibachi and guide field requirements. A base line design for a 50 kJ module including amplifier staging, layout and overall size is discussed. Cost analysis and scaling for optical components, pulsed power technology and the guide field are also presented.

TABLE OF CONTENTS

Section		Page
I	INTRODUCTION	1
II	CONCEPTUAL OPTICAL DESIGNS	6
	2.1 Overall Concepts	6
	2.1.1 Design Philosophy	6
	2.1.2 Optical Design Considerations	7
	Extraction Arrays, Volume Utilization	7
	Simple Angular Multiplexing	10
	More Efficient Angular Multiplexing	12
	Damage Threshold Limitations	14
	Spherical Focusing Element	15
	Cylindrical Focusing Elements	15
	Amplifier Aspect Ratio	16
	2.1.3 Beam Array Trade-Offs	20
	Beam Separation	22
	2.1.4 Alignment	30
	2.1.5 Encoding	30
	2.1.6 Pupil Relaying and Spatial Filtering	31
	2.1.7 Spherical Optics	34
	2.2 Optical Quality Budget	36
	2.2.1 Single Beam Line Effects	36
	Optical Elements	40
	Beam Propagation (Beam Steering Errors)	40
	Beam Propagation (Effects of Turbulence)	44
	Nonuniform Gain Medium Effects on Optical Quality	46
	Absorbing Species	48

Section	Page
2.2.2 Multiple Beam Irradiance Effects	50
Incoherent Addition of Subapertures	50
Coherent Addition of Subapertures	57
2.3 Windows for the Power Amplifier	63
2.3.1 Materials Choices	63
2.3.2 Window Segmentation	67
Brewster Angle Windows	68
Segmented Windows and Tolerances on Wedge	70
Structural Rigidity and Segmentation	71
Frames for Segmented Windows	75
2.3.3 Window Heating	77
Energy Absorbed Per Pulse	77
Temperature Rise Per Pulse	77
Transient Response	78
Convection	80
Optical Effect of Window Heating	81
2.3.4 Shock Effects	83
2.3.5 Aerodynamic Windows	84
Full Aperture Aerodynamic Windows	84
Reduced Aperture Aerodynamic Windows	84
Alignment Constraints	88
2.4 Prepulse Suppression	91
2.4.1 Surface Scatter	91
2.4.2 Diffraction	95
2.4.3 Spontaneous Emission	98
2.4.4 Nonuniformities in the Amplifier Medium	100
2.5 Optical System Configuration and Alignment	101
2.5.1 Optical Configuration Design Logic	104
Design Performance	110

Section		Page
	2.5.2 Optical System Alignment	120
	2.6 Section II References	128
III	KINETICS AND AMPLIFIER SCALING	130
	3.1 KrF and XeCl Lasers	130
	3.2 Kinetic Modelling of KrF	132
	3.2.1 E-Beam Ionization and Excitation	132
	3.2.2 Reaction Mechanisms and Radiative Properties	133
	3.2.3 Temperature Dependent Rates	136
	3.2.4 Code Predictions Compared with Experiments	136
	3.2.5 Key Areas of Uncertainty in KrF* Laser Kinetics	146
	3.3 Extraction Efficiency: Nonsaturable Absorption	150
	3.3.1 Single Pass Extraction	150
	3.3.2 Double Pass Extraction	151
	3.4 Amplified Spontaneous Emission (ASE)	158
	3.4.1 Extraction Efficiencies with ASE	162
	3.5 Parasitic Limitations	174
	3.6 Scaling Maps for Large KrF Amplifiers	177
	3.7 Amplifier Layout	191
	3.8 Section III References	192
IV	CRITICAL OPTICAL TECHNOLOGY	193
	4.1 Fabrication	193
	4.1.1 Mirrors	193
	4.1.2 Windows	194
	4.2 Optical Coatings	195
	4.3 Section IV References	198
V	ELECTRON GUN TECHNOLOGY	199
	5.1 Introduction	199
	5.2 Scoping Calculations	201

Section	Page
5.2.1 Beam Voltage	202
5.2.2 E-Beam/Diode Performance	203
5.2.3 Self Magnetic Field	203
5.2.4 Pulsed Power Requirements	203
5.3 Cathode Design	206
5.4 E-Beam Energy Deposition Uniformity	208
5.5 F-Beam Window and Support Structure	216
5.5.1 Stress Analysis: Foil and Hibachi	220
5.5.2 Stress Analysis: Support Structure	221
5.6 Magnetic Guide Field	223
5.7 Segmented Cathodes	230
5.7.1 Segmentation Limits	230
5.7.2 Beam Drift Considerations	233
5.8 Section V References	236
VI POWER CONDITIONING	237
6.1 PFN and PFL Scaling Relations	237
6.1.1 PFN Scaling	237
6.1.2 PFL Scaling (Single Line)	244
6.1.3 PFL Scaling (Multiple Lines)	246
6.1.4 PFN/PFL Comparison	249
6.2 Magnetic Guide Field Technology	252
6.2.1 Design Description	253
6.2.2 Magnet Analysis	258
VII BASE LINE DESIGN	261
7.1 Optical Train Staging for 50 kJ Modules	261
7.1.1 Staging	261
7.1.2 Damage Threshold and System Size	262
Windows	262
Mirrors	264
Overall Length	264

Section	Page
7.2 Trade Offs and Cost Analysis	265
7.2.1 E-Beam Pulsed Power and Guide Field Costs	265
System Efficiencies	265
Cost Scaling Procedure	267
Diode: Cost Scaling	267
Guide Magnetic Field: Cost Scaling	267
Pulsed Forming Lines: Cost Scaling	268
Pulsed Forming Networks: Cost Scaling	270
System Cost Scaling Relationships	271
7.2.2 Optical Materials and Fabrication	273
Elements with Apertures Under 60cm: Cost Scaling	275
Large Optical Elements: Cost Scaling	278
7.2.3 Coatings	270
Cost Data	280
REFERENCES	287
APPENDICIES	290
A Kinetic Rate Constants	290
B ASE Flux Computations	296
C ASE Spectral Width Considerations	301
D Fluid Dynamic Analysis of Absorbing Curtain Flows	305

LIST OF FIGURES

Figure		Page
1.1-1	Conceptual Arrangement for KrF Scaling Module	2
2.1-1	Some Possible Extraction Arrays from the Final Amplifier	8
2.1-2	Simple Rectangular Final Amplifier Geometries Illustrating the Volume Left Unextracted by Angular Multiplexing	9
2.1-3	The Fractional Final Amplifier Volume Left Unextracted for Simple Angular Multiplexing	11
2.1-4	Beam Reducing Arrangement that Shortens the Path Length Required for Beam Separation	13
2.1-5	Percent Volume Left Unextracted Versus the Number of Beam Lines for a Variety of Extraction Arrays With and Without Focusing	18
2.1-6	Comparison of Beam Path Length Needed for Beam Separation Versus the Percent Volume Left Unextracted	19
2.1-7	Linear Beam Array Relaying. Each of the N Beams Requires its Own Relay Optics Which are Unique for Each of the Beams	21
2.1-8	Pre-separation of Beams to Control Angular Field	23
2.1-9	Beam Packing Arrays that are (a) Conical, (b) Nested Conical, and (c) a 180° Sub-arc of Nested Conical Beam Distributions.	25
2.1-10	Conical, Nested Conical and 180° Sub-arc of a Nested Conical Array.	27
2.1-11	A Tightly Packed Array of Beams Contained Within a Half Circle, Radius R_4 , and the Radii of a Conical Distribution (R_1), and a 180° Sub-arc of a Nested Conical Distribution (R_3)	28

Figure		Page
2.1-12	Generic Encoder Concept	32
2.1-13	Generic Encoder Concept System Block Diagram	33
2.1-14	Pupil Relaying Schematic	35
2.2-1	PAM Energy Constraints Set by Optical Quality, Damage Limitations, Target Chamber Geometry and Maximum Optical Window Size for a 1 MJ System	39
2.2-2	Beam Steering Caused by Thermal Gradients	41
2.2-3	Schematic of Optical System for Separating, Delaying and Focusing to Target	52
2.2-4	Subaperture Beam Irradiance of the Fusion Pellet	53
2.2-5	Maximum $fN^{\frac{1}{2}}$ Product for a Given Focal Spot Size, x_d , as a Function of Final Amplifier Window Element Size, D/n	56
2.2-6	Starting Wavefront of the Passage through the Segmented Window	59
2.2-7	(a) - (b) Pellet Irradiance Due to a Single Beam Passing through a 4 x 4 Segment Window with Thickness Errors Varying from Zero to $\lambda/4$	60
2.2-7	(c) - (d) Pellet Irradiance Due to a Single Beam Passing through a 4 x 4 Segment Window with Thickness Errors Varying from Zero to $\lambda/4$	61
2.2-8	(a) - (b) Pellet Irradiance Due to Multiple Beam Illumination	62
2.3-1	A Brewster Angle Window Bowing Under Pressure	69
2.3-2	(a) Window Thickness Required for a Segmented Window Where the Maximum Design Stress is 1000 psi	72
2.3-2	(b) Window Thickness Requirements for Segmented Simply Supported Windows	73
2.3-2	(c) Window Thickness Requirements for Segmented Clamped Windows	74

Figure		Page
2.3-3	Support Frame for an $n \times n$ Segmented Window	76
2.3-4	Temperature Rise in Steady State across a Quartz Window Receiving 2.1 J/cm^2 Irradiance at 10 Hz	79
2.3-5	Transient Thermal Response of a 2.5 cm Thick Window Receiving 2.1 J/cm^2 Irradiance at 10 Hz	81
2.3-6	Transient Thermal Response of a 5 cm Thick Window Receiving 2.1 J/cm^2 Irradiance at 10 Hz	82
2.3-7	Optical Design of an Amplifier Utilizing a Non Material Window	86
2.3-8	An Optical Path Difference (opd) Map for One Beam of a Conical Beam Array After Passing Through a Pair of $f/4$ 1 Meter Parabolic Cylinders	89
2.4-1	Mirror and Spatial Filter For Control of Beam Quality	93
2.4-2	Diffraction at a Circular Aperture	96
2.5-1	Base Line Block Diagram	102
2.5-2	Basic Elements of the Module Optical System	103
2.5-3	The Assembled Nested Optical System	105
2.5-4	Folded Optical System Using Full Circular Arrays of Beams	106
2.5-5	Optical System (Module) Length and Beam Separation Distance as a Function of Output Angle for Various Compression Ratios (N)	112
2.5-6	Requirements for Pre-separators as a Function of Output Field Angle	114
2.5-7	Aberration Effects for Spherical Relay Mirrors	115
2.5-8	Folded Optical System Using Full Circular Arrays of Beams	116

Figure		Page
2.5-9a	Pellet Irradiance by a Single Beam Without Aberrations.	117
2.5-9b	Pellet Irradiance by a Single Beam With 3 Waves of Astigmatism	118
2.5-9c	Pellet Irradiance by a Single Beam With 1.3 Waves of Astigmatism	119
2.5-10a	One Quadrant of the Pellet Irradiance Due to 84 Beams Illumination. Each Fully Corrected	121
2.5-10b	One Quadrant of the Pellet Irradiance Due to 84 Beam Illumination. Each With 3 Waves	122
2.5-10c	One Quadrant of the Pellet Irradiance Due to 84 Beam Illumination. Each With 1.3 Waves	123
2.5-11	Alignment of the Encoder to the First Pre-amplifier Through a Pre-separator Showing (a) Angular Alignment to the Pre-separator Assembly, (b) Angular Field Alignment, and (c) Final Pointing Control	125
2.5-12	Beam Transfer and Alignment to the Next Stage for a Folded Optical System	126
3.2-1	Major Formation and Quenching Kinetics for Typical KrF* Laser Mixture	134
3.2-2	MSNW KrF Code Matches Fluorescence Data	139
3.2-3	MSNW KrF Code Results for F ₂ Burn-up Compared to Experimental Measurements	141
3.2-4	MSNW KrF Code Matches the Time History of Oscillator Output Power Measurements	144
3.2-5	MSNW KrF Code Predictions of Oscillator Power Output Compared with Sandia Measurements	145
3.2-6	Model of Kinetic Formation Paths Within the KrF* Manifold	147
3.2-7	Effect of Finite Vibrational Relaxation and B-C Exchange Rates in the MSNW KrF Code	148

Figure		Page
3.2-8	Effect of KrF Vibrational Relaxation and B-C Exchange on Amplifier Efficiency	149
3.3-1	Optical Extraction Efficiency in Single Pass Amplifier	152
3.3-2	Optical Extraction Efficiency in Double Pass Amplifier with Nonsaturable Absorption	154
3.3-3	Flux Distribution in Single and Double Pass Amplifiers with Nonsaturable Loss	155
3.3-4	Maximum Extraction Efficiency in a Double Pass Amplifier with Nonsaturable Loss	156
3.4-1	Geometry Used in Computing the ASE Flux	160
3.4-2	Extraction Efficiencies for Single and Double Pass Amplifiers Versus g_0L with and without ASE	163
3.4-3	Extraction Efficiencies for Single and Double Pass Amplifiers Versus the Input Flux, $\beta(\text{in})$	164
3.4-4	Extraction Efficiency Versus the Amplifier Aspect Ratio	166
3.4-5	Local Flux and Gain Distributions in Single and Double Pass Amplifiers Without ASE Effects	167
3.4-6	Local Flux and Gain Distribution in Single and Double Pass Amplifiers With ASE Effects	168
3.4-7	Geometry Illustrating the Increased ASE Flux Due to a Large Mirror Used to Double Pass the Amplifier	170
3.4-8	The Reduction in Extraction Efficiency Caused by Reflection of ASE Flux by a Large Mirror Used for Multiple Pass Extraction is Illustrated in These Plots of η_{ext} Versus the Gain/Loss Ratio	171
3.4-9	Extraction Efficiency Versus the Gain-to-Loss Ratio for a Variety of g_0L and $\beta(\text{in})$ Values	172
3.4-10	Operating Map Based on Extraction Efficiency Computations	173

Figure		Page
3.5-1	Maximum Value of the Amplifier Loaded Gain Versus the Input Flux	175
3.5-2	Maximum Allowable Wall Reflectivity Versus $g_0 L$ for a Variety of Input Flux Levels	176
3.5-3	Possible Geometry for an Absorbing Gas Curtain for Parasitic Control	178
3.6-1	Time Dependent Parameters for an Amplifier Operating at 1 atm with 0.5% F_2 , and Pumped at 300 kW/cc	181
3.6-2	Typical Time Dependence of the Extraction Efficiency and Amplifier Stored Power Density	182
3.6-3	Extractable Energy Density for Amplifiers Operating at 1 and 2 atm, and Over a Wide Range of Pumping Rates and Run Times	183
3.6-4	Scaling Map Format for 1 and 2 atm Pressure Amplifiers	185
3.6-5	Scaling Maps for 1 and 2 atm Pressure Amplifiers With Aspect Ratios of Unity	187
3.6-6	Pulses Power Operating Bounds Cross Plotted on Scaling Maps for 1 and 2 atm Amplifier Modules	190
5.1-1	PAM System Schematic Illustrating the e-Beam/ Pulsed Power Subsystems	200
5.3-1	Typical Voltage and Current Waveforms from the DARPA Rep-Rate Facility Using a Carbon Felt Cathode	207
5.4-1	Electron Range in Neon and Argon	209
5.4-2	Segmented Gain Volume for Evaluating Electron Loss	211
5.4-3	Energy Deposition as a Function of Distance into the Laser Cell	212
5.4-4	Energy Deposition as a Function of Distance into the Laser Cell	213
5.5-1	Foil/Hibachi Design for a 50 kJ, 2 atm PAM	217

Figure		Page
5.5-2	Cross Section Illustrating the Foil Window, the Hibachi and the Hibachi Support	218
5.5-3	Details of the Hibachi Module	219
5.6-1	Calculated Trajectories for a 50 kJ PAM, 500 ns Diode	225
5.6-2	Hibachi Interception Due to Beam Pinch	227
5.6-3	Hibachi Interception Due to Beam Pinch	228
5.6-4	Hibachi Interception Due to Beam Pinch	229
5.7-1	Segmented Diode with Beam Expansion	231
6.1-1	Two Mesh Guillemin Type A PFN	238
6.1-2	PFN Scaling Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length	241
6.1-3	Maximum Number of Parallel Modules vs Beam Width	243
6.1-4	Plot of Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length. Single Line	247
6.1-5	Plot of Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length. Multiple Lines	250
6.1-6	Comparison of PFN and PFL Technology at 50 kJ Design Point	251
6.2-1	Side View of Superconducting Helmholtz Coil Pair	254
6.2-2	End View of Guide Field Coils	255
6.2-3	Cross Section of Superconducting Coil	256
7.1-1	Possible Staging Arrangement for a 50 kJ Final Amplifier System	263
7.2-1	Pulsed Power System Costs	274
7.2-2	Cost per Square Inch as a Function of Clear Aperture. For a Square Aperture Costs Must be Multiplied by $\pi/2$	277

Figure		Page
7.2-3	Coating Costs for Smaller Optics	282
7.2-4	Cost per Square Inch as a Function of Clear Aperture	284
7.2-5a	Equipment and Facility Costs for Large Optics Coating Chambers	285
7.2-5b	Coating Chamber Cost as a Function of the Clear Aperture of the Optical Element it can Accomodate	286

LIST OF TABLES

Table	Page
2.1-1 Maximum Angular Deviation and Fractional Volume Left Unextracted for Various Extraction Arrays.	17
2.5-1 Minimum Size Optical System Designs.	111
3.1-1 Comparison of Published Experimental Results on KrF and XeCl.	131
3.2-1 Formation and Quenching Fractions for a Typical KrF* Laser Mixture.	135
3.2-2 Comparison of Measured and Code Calculated Absorption Coefficients.	137
3.2-3 Comparison of Power Extracted in Laser Experiments with the Code Predicted Results.	142
3.5-1 Absorbing Curtain Flow Parameters for a 100 cm x 100 cm e-Beam Foil Aperture.	179
5.2-1 Pulsed Power Operating Conditions for a 50 kJ, 1 atm, Amplifier.	204
5.2-2 Pulsed Power Operating Conditions for a 50 kJ, 2 atm, Amplifier.	205
6.2-1 Guide Field Magnet Parameters.	260
7.2-1 Efficiency Breakdown for Pulsed Power Options.	266
7.2-2 Costs for Multilayer uv Optical Coatings.	283

I. INTRODUCTION

This report describes the development of a conceptual design of a nominal 50 kJ KrF laser with an output pulse length of the order of 10 nsec. It is envisioned that such a laser module would be replicated to supply the high pulse energy, of the order of 1MJ, in the development of an inertial confinement fusion (ICF) reactor driver with sufficient peak power to reach breakeven conditions. The present design concept is based upon the use of a long pulse KrF laser amplifier (pulse length typically 500 nsec) with simple geometrical pulse compression to produce the required pulse length of the order of 10 nsec. This concept permits operation of the scalable rare gas halogen (RgH) excimer lasers at pulse lengths where they have proven high intrinsic energy.

The 50 kJ conceptual design study involved three basic technologies; laser engineering, optical engineering and electrical power conditioning and the work was completed by a team including Mathematical Sciences Northwest, Inc., Perkin-Elmer and Maxwell Laboratories, Inc. Mathematical Sciences was responsible for the laser kinetics amplifier scaling and overall program direction. Perkin-Elmer had responsibility for the system optical design and analysis, while Maxwell Laboratories was responsible for the e-beam/deposition and pulsed power engineering for the amplifier module.

A major objective of the present design study was to emphasize the use of low risk, current technology in order to enhance rapid engineering development of the nominal 50 kJ scaling module which might then be replicated to the desired energy level. Considerable attention has therefore been given to such engineering issues as scalability of electron beam excitation source for the RgH laser, the development of the large optical elements necessary for the power amplifier, and the design constraints imposed by damage limits to optical surfaces. Another effect which influences the development of the optical design concept was the engineering feasibility of the optical alignment procedures which would be necessary to ensure efficient and economical operation of each scaling module. Although illumination of the target was not

Figure 1.1-1. Conceptual Arrangement for KrF Scaling Module.

included in the present design study, consideration has been given to the associated beam quality necessary in the optical system to ensure that our conceptual design would permit uniform and efficient illumination of the target.

The basic conceptual design which was used in this study for pulse compression of the RgH laser is shown in Figure 1.1-1 and utilizes the concept of angular multiplexing and beam stacking. A master oscillator supplies the initial nominal 10 nsec pulse to an angular multiplexer (encoder) which produces N equally intense 10 nsec beams that are sequentially spaced in time to form a 10 nsec pulse. Each of the N beams, although sequentially spaced, is individually distinguishable and separable by virtue of angular encoding. An optical imaging system expands and transfers the N beams to each amplification stage. The imaging optics permit the use of spatial filters to clean up each beam. The N beams are then separated spatially with a combination beam converger/separator system and individually directed to the target by appropriate delay to temporally stack the N beams to a single 10 nsec pulse.

A primary consideration in the design of the optical system is the necessity to extract energy from the largest possible fraction of the preamplifier and the final amplifier volume. This is essential to limit the amplified spontaneous emission (ASE) and parasitic oscillations from robbing energy from the high gain amplifier volume. Three different extraction arrays were considered including linear, square, and circular packing in the final amplifier. The fractional volume left unextracted with each of the beam extraction arrays is described in Section II. The linear array with cylindrical focusing elements exhibits the most efficient volume extraction but has been rejected because of the cost and complexity of developing the large cylindrical optical elements necessary for the final amplifier and subsequent beam separation. The circular extraction array was the ultimate choice in the design study as a compromise between efficient illumination of the amplifier volume and simplification in the optical alignment of the entire system. By virtue of the axis of symmetry and the use of spherical optical surfaces,

all N beams are identical and a single alignment system can access each beam as described in Section II.

Detailed laser kinetic calculations were made in order to develop scaling maps. These calculations are presented in Section III and can be used to size the final amplifier in the scaling module and to determine the number of preamplifiers. These scaling maps were derived from calculations using a kinetic modelling code which was developed and validated against all the available experimental data ranging from short pulse high current density to long pulse low current density experiments. The effects of ASE have been included in the calculations presented in Section III. The losses due to ASE dictate the use of multiple pass extraction of each amplification stage in order to maintain reasonable extraction efficiencies of approximately 40 percent. These kinetic modelling calculations were also used to determine the sensitivity of the extraction efficiency to the gain-to-loss ratio intrinsic to the KrF medium. A general rule of thumb emerged from the calculations that the gain-to-loss ratio should exceed 10 during the excitation pulse in order to obtain an extraction efficiency of 40 to 50 percent. Finally, some consideration has been given to the effect of parasitics which tend to limit the achievable power gains in large amplifiers to the range of 10 to 20.

In keeping with the design objective emphasizing low risk current technology, the detailed optical design of the system was based on the use of flats and spheres for the major optical elements. An assessment of the critical optical technology, associated with the scaling module, is presented in Section IV. This assessment also takes into account present day fabrication capability and limitations. This has led in the direction of proposing the use of segmented optics for large elements, e.g., the final amplifier window and mirror. Another major issue in the optical design concerns the damage-fluence limits for both coatings and optical substrate. Based on the limited available data, we have chosen short and long pulse fluence limits of 2 J/cm^2 and 5 J/cm^2 (corresponding to pulse lengths on the order of 10 nsec and 500 nsec respectively). This fluence limit determined the size of the optical elements after final beam separation and in the beam stacking region (see Figure 1.1-1).

Electron beam gun technology, related deposition problems and power conditioning are reviewed in Sections V and VI. Consideration of the uniformity of e-beam deposition dictates the use of two sided electron beam pumping. Some simple scaling laws were developed for electron guns driven either by pulse forming networks (PFN) or pulse forming waterlines (PFL). These results, combined with the kinetic scaling calculations, have been used to identify the different operation regions for both PFNs and PFLs. Beam pinch effects due to the self magnetic field of broad, high current density e-beams are an inherent problem in the design of the scaling module. The beam pinch can be controlled by the use of an external guide (B) field or by subdividing the electron gun into small areas with individual return current to limit the self magnetic field. The energy required to provide the guide B-field scales as B^2 and hence as the square of the product of the current density and the e-beam width. Some consideration has therefore been given to techniques for the efficient segmentation of the e-beam cathode in order to limit the magnitude of the guide field.

The basic conclusions of the study are presented in Section VII including the baseline design for a 50 kJ scaling module. Several major technology issues have been identified including the optical damage limit and e-beam cathode segmentation. The damage fluence limit clearly determines the size of the separation optics and for scaling modules larger than 50 kJ may also limit the design of the amplifier aperture. The cost of the guide field associated with single cathode e-beam appears to be a significant fraction of the scaling module engineering cost. This emphasizes the ultimate necessity to determine the technology limits in developing efficient segmented e-beams. Notwithstanding these technology problems, it seems clear from the present conceptual design study that a 50 kJ scaling module could be built with existing technology to provide a pulse compression of up to approximately 50. It is concluded that the basic design study could be readily demonstrated with existing technology.

II. CONCEPTUAL OPTICAL DESIGNS

2.1 Overall Concepts

2.1.1 Design Philosophy

Angular multiplexing for pulse amplification and compression requires an optical system which transfers a single short duration pulse through a spatial/temporal encoder, preamplifiers, a final power amplifier pulse stacker and focusing optics to a fusion pellet. In the pure angular multiplexing concept, the pulse compression ratio is equal to the number of beam lines which pass through the common chain of amplifiers. With a large compression ratio, a large number of optical beam lines is required and this fact motivates the design philosophy. With large numbers of beam lines it is of paramount importance to limit the number of optical elements, develop a design that has a minimal sensitivity to alignment errors due to structural motion and displacements by judicious location of critical optics and subsystems, and to use common optical elements in each beam line.

To minimize the number of optical elements and simplify alignment, it is desirable to design the optical system such that all N beams share common optical elements whenever possible. When this is not possible, it is equally important to use identical optics in each beam line to facilitate fabrication, assembly, maintenance and to minimize cost. Building the system based on an axisymmetric design and using circular or conical beam arrays minimizes aberrations in the system and facilitates the use of common optical elements for each beam. Employing imaging relaying between amplifier stages allows the alignment scheme to be essentially independent of the number of beam lines and this leads to a major simplification of the alignment task.

The use of spherical and flat optical elements is advantageous from the point of view of cost, beam quality, cross talk and alignment.

The cost of fabrication and metrology of large aspheric and of off-axis surfaces is much higher than for spherical surfaces; further aspheric and off axis elements generally have poorer optical figure and surface roughness (at short wavelengths) compared to spherical and flat elements. This poorer surface roughness will lead to more severe scattering of the beams and result in enhanced cross talk problems. From the point of view of alignment, the spherical optical element has great advantages over aspheric and off-axis elements.

As a result of these considerations, the general philosophy has been to use common optics (spheres and flats), common beam lines, image relaying, optically axisymmetric geometries and circular beam arrays whenever possible. The benefits from following these general rules will become apparent in the following sections.

2.1.2 Optical Design Considerations

Extraction Arrays, Volume Utilization

There are a great variety of geometrical arrangements for extracting N beams from the final amplifier. Three possible extraction arrays are shown in Figure 2.1-1 including linear, square, and circular arrangements. The cross section of an individual beam is shown as a square, but other shapes (rectangular, for example) are certainly possible.

Figure 2.1-2 shows two simple rectangular final amplifier geometries; the volume, in each case, is being extracted by a single beam that propagates with an angular deviation to the optical axis of $\theta_{\max}$. For the following discussion, the angle $\theta_{\max}$ will be the maximum angle that an individual beam makes with the final amplifier optical axis. It is straightforward to write down expressions for the fractional volume left unextracted in terms of $\theta_{\max}$:

$$\text{Linear Array: } \frac{\Delta V}{V} = \frac{L\theta_{\max}}{2H} \quad (2.1-1)$$

LINEAR

SQUARE

CIRCULAR

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Figure 2.1-1. Some Possible Extraction Arrays from the Final Amplifier

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Figure 2.1-2. Simple Rectangular Final Amplifier Geometries Illustrating the Volume Left Unextracted by Angular Multiplexing

$$\text{Square Array: } \frac{\Delta V}{V} = \frac{L\theta_{\max}}{W} \quad (\text{Square beams } W=H) \quad (2.1-2)$$

$$\text{Circular Array: } \frac{\Delta V}{V} = \frac{L\theta_{\max} \sqrt{H^2 + W^2}}{2HW} \quad (2.1-3)$$

Simple Angular Multiplexing

The simplest form of angular multiplexing involves sequentially passing short pulses through the final amplifier at slightly different angles; the various beams are then separated by allowing them to propagate over large distances until each beam can be separately redirected towards the target with an optical element that must have the full amplifier aperture. The propagation path, ℓ , needed to separate two adjacent beams is related to their angular deviation, θ , as

$$\ell = \frac{D}{\theta}, \quad (2.1-4)$$

where D is the beam dimension in the direction of the angular multiplexing.

Because the beams must fill a large part of the final amplifier to control parasitics and ASE, and to extract a large fraction of the stored energy, their angular separation must be kept quite small; consequently, the propagation length for beam separation becomes very large.

Figure 2.1-3 illustrates this straightforward angular multiplexing technique and lists values of $\theta_{\max}$ for the three extraction arrays under consideration. The fractional volume left unextracted is also listed for each case (here we have taken the individual beams to have a square cross section for simplicity only); we note the increase in unextracted volume as the number of beams grows. The square extraction array shows a weaker dependence on N ($N^{1/2}$ versus N) because of the more efficient packing of the square array.

ARRAY	θ_{max}	$\frac{\Delta V}{V}$
LINEAR (RECTANGULAR BEAMS)	$\frac{ND}{2\ell}$	$\frac{NL}{4\ell}$
SQUARE (SQUARE BEAMS)	$\frac{\sqrt{N} D}{2\ell}$	$\frac{\sqrt{N} L}{2\ell}$
CIRCULAR (SQUARE BEAMS)	$\frac{N D}{\sqrt{2\pi}\ell}$	$\frac{N}{2\pi} \quad \frac{L}{\ell}$

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Figure 2.1-3. The Fractional Final Amplifier Volume Left Unextracted for Simple Angular Multiplexing.

These simple multiplexing schemes have several disadvantages. First, each beam must be collected with an optical element as large as the final amplifier aperture itself; and second, to minimize the fractional volume left unextracted, very long propagation paths are necessary. For example, suppose the amplifier length is 1.0 meters; the path length required to separate 50 beams and leave only 1 percent of the amplifier volume unextracted is 1250 meters for the linear array, 353 meters for the square array and 795 meters for the circular array.

More Efficient Angular Multiplexing

The next step in producing more efficient angular multiplexing arrangements (more efficient amplifier volume extraction) is to use a focusing element at the output window to reduce the size of the individual angular multiplexed beams. The possibility of reducing the beam area greatly shortens the propagation path length needed for beam separation, or alternatively, keeping the path length constant, we greatly reduce the angular deviation between adjacent beams in the amplifier reducing the fractional volume left unextracted. There are several constraints which will limit just how much improvement can be achieved; for example, the quality of the optical elements will set a lower limit on the angular deviation possible between adjacent beams with the ultimate limit being fixed by diffraction; a more stringent requirement will be the damage threshold limits which must not be exceeded at any optical surface.

For the present, we first consider the potential benefits of reducing the individual beam diameters and then impose the restrictions of damage thresholds.

Figure 2.1-4 shows the geometry involved in a beam reducing scheme using a focusing lens, of focal length f , as the output window of the final amplifier. The amplifier has a square cross sectional area of D^2 and a length L . The angular deviation between two adjacent

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Figure 2.1-4. Beam Reducing Arrangement that Shortens the Path Length Required for Beam Separation

beams is θ and is given by $\theta = d/\ell$ where ℓ is the propagation distance for beam separation. The maximum angular beam deviation, $\theta_{\max}$, from the amplifier axis will depend on θ , the number of beams N , and the beam extraction array geometry.

$$\theta = \frac{d}{\ell} \quad (2.1-5)$$

$$\ell = f(1-d/D) \quad (2.1-6)$$

$$f = \frac{d}{\theta(1-d/D)} \quad (2.1-7)$$

From Equations (2.1-4,5), it is clear that focusing the beam decreases the path length for beam separation by the ratio (d/D) ; or keeping the path length constant, we can reduce θ , and therefore, $\theta_{\max}$, by the same factor.

Damage Threshold Limitations

We now impose the condition that the short pulse damage threshold, ϵ_{th} (J/cm²), not be exceeded at any optical element. The total energy to be extracted from the amplifier is E_T so that the long pulse energy density at the output lens is $(E_T/A) \equiv \epsilon_o$, where A is the final amplifier window area. The long pulse length is $N\tau$ so that each of the N multiplexed beams has a temporal width of τ . The individual N beams when separated at a distance ℓ from the amplifier will have a cross section of area a . The energy density in each of these N beams, given by (E_T/Na) , must not exceed the short pulse threshold for damage at the mirror or lens used to redirect the converging beams. The threshold ϵ_{th} will be some function of the exposure time, τ , and the material properties. It is expected that $\epsilon_o/\epsilon_{th} \geq 1$.

Before proceeding to calculate the fractional volume left unextracted in a focusing system, one should note that the output focusing element may be spherical or cylindrical in nature. A spherical element may be used with a linear, square or circular extraction array while the cylindrical element would normally be restricted to the linear extraction array. To illustrate the general properties of the focusing

arrangement, we have chosen to consider, for the present, N angular multiplexed beams, each with a square cross sectional area. This restriction is for simplicity only and does not restrict the application of this technique.

Spherical Focusing Element

Each individual beam will be reduced in size to an area a ; the angular separation between adjacent beams is $\theta = \sqrt{a}/\ell$. The damage threshold limitation sets a lower limit on the reduced beam area a of:

$$a \geq \frac{E_T}{N\epsilon_{th}} = \frac{A\epsilon_0}{N\epsilon_{th}} \quad (2.1-8)$$

This condition is now used to set a lower limit on the maximum angular deviation of a beam from the amplifier.

$$\text{Linear Array:} \quad \theta_{\max} = \frac{N\sqrt{a}}{2\ell} \geq \frac{\sqrt{A}}{2\ell} \left(\frac{N\epsilon_0}{\epsilon_{th}} \right)^{1/2} \quad (2.1-9)$$

$$\text{Square Array:} \quad \theta_{\max} = \frac{\sqrt{Na}}{2\ell} \geq \frac{\sqrt{A}}{2\ell} \left(\frac{\epsilon_0}{\epsilon_{th}} \right)^{1/2} \quad (2.1-10)$$

$$\text{Circular Array:} \quad \theta_{\max} = \frac{N\sqrt{a}}{2\pi\ell} \geq \frac{\sqrt{2A}}{2\pi\ell} \left(\frac{N\epsilon_0}{\epsilon_{th}} \right)^{1/2} \quad (2.1-11)$$

Cylindrical Focusing Element

Each individual beam will now only be reduced in size in one dimension so that the angular separation between adjacent beams is still $\theta = d/\ell$, but the damage threshold limitation is written as

$$dD \geq \frac{E_T}{N\epsilon_{th}} = \frac{D^2}{N} \left(\frac{\epsilon_0}{\epsilon_{th}} \right), \quad (2.1-12)$$

or

$$d \geq \frac{D}{N} \left(\frac{\epsilon_0}{\epsilon_{th}} \right). \quad (2.1-13)$$

The maximum angular deviation of a beam from the amplifier axis is now

$$\text{Linear Array: } \theta_{\max} = \frac{Nd}{2\ell} \geq \frac{D}{2\ell} \left(\frac{\epsilon_o}{\epsilon_{th}} \right) . \quad (2.1-14)$$

The fractional volume left unextracted with the focusing system is found using the expressions for $(\Delta V/V)$ in Equations (2.1-1) to (2.1-3); the results are tabulated in Table 2.1-1 where the previous unfocused results are also listed for comparison. Figure 2.1-5 shows a plot of $(\Delta V/V)$ versus the number of beams N for various extraction arrays and focusing elements. Note that only the cylindrically focused linear array and the spherically focused square array give results independent of N ; however, the spherically focused circular array gives the next best results for all values of $N \geq 8$. The plots in Figure 2.1-5 were constructed assuming $(\epsilon_o/\epsilon_{th}) = 2.5$, $\ell = 100$ meters and an amplifier length, L , of 1 meter.

In particular, it should be pointed out that the use of a focusing element greatly reduces the propagation length needed for beam separation. Recall, the circular array with no focusing required a path length of 795 meters to separate 50 beams and keep $(\Delta V/V)$ below 0.01; when spherical focusing is used this distance is reduced to 300 meters. This comparison is shown for other values of $(\Delta V/V)$ in Figure 2.1-6.

Amplifier Aspect Ratio

From the point of view of extracting energy from the maximum amplifier volume, it is clearly advantageous to reduce the amplifier length. Since the amplifier total volume is a constant, for a given total output energy, we set

$$\begin{aligned} LD^2 &= V_o \\ E_T &= \epsilon_o D^2 \end{aligned} \quad (2.1-15)$$

Table 2.1-1. Maximum Angular Deviation and Fractional Volume Left Unextracted for Various Extraction Arrays.

	EXTRACTION ARRAY	$\theta_{\max}$	$(\Delta V/V)$
SPHERICAL FOCUSING ELEMENT	Linear	$\frac{D}{2\ell} \left(\frac{N\epsilon_0}{\epsilon_{th}} \right)^{1/2}$	$\frac{L}{4\ell} \left(\frac{N\epsilon_0}{\epsilon_{th}} \right)^{1/2}$
	Square	$\frac{D}{2\ell} \left(\frac{\epsilon_0}{\epsilon_{th}} \right)^{1/2}$	$\frac{L}{4\ell} \left(\frac{4\epsilon_0}{\epsilon_{th}} \right)^{1/2}$
	Circular	$\frac{D}{\sqrt{2}\ell} \frac{1}{\pi} \left(\frac{N\epsilon_0}{\epsilon_{th}} \right)^{1/2}$	$\frac{L}{4\ell} \left(\frac{4N}{\pi} \frac{\epsilon_0}{\epsilon_{th}} \right)^{1/2}$
CYLINDRICAL FOCUSING ELEMENT	Linear	$\frac{D}{2\ell} \left(\frac{\epsilon_0}{\epsilon_{th}} \right)$	$\frac{L}{4\ell} \left(\frac{\epsilon_0}{\epsilon_{th}} \right)$
NO FOCUSING ELEMENT	Linear	$\frac{D}{2\ell} (N)$	$\frac{L}{4\ell} (N)$
	Square	$\frac{D}{2\ell} (N)^{1/2}$	$\frac{L}{4\ell} (2\sqrt{N})$
	Circular	$\frac{D}{\sqrt{2}\ell} \left(\frac{N}{\pi} \right)$	$\frac{L}{4\ell} \left(\frac{2N}{\pi} \right)$

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Figure 2.1-5. Percent Volume Left Unextracted Versus the Number of Beam Lines For a Variety of Extraction Arrays With & Without Focusing.

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Figure 2.1-6. Comparison of Beam Path Length Needed for Beam Separation Versus the Percent Volume Left Unextracted.

These expressions assume a square beam cross section of area D^2 . The fractional volume left unextracted, as listed in Table 2.1-1 can be re-written by eliminating L and ϵ_0 using Equation (2.1-15). Making this substitution, the cylindrically focused linear array shows a D^{-4} dependence, while the spherically focused arrays show a D^{-3} dependence. Clearly, a slight increase in the amplifier aperture greatly reduces the amplifier volume left unextracted.

2.1.3 Beam Array Trade-offs

In the previous section, several angular multiplexing extraction array geometries have been compared in terms of the fractional amplifier volume left unextracted and the beam propagation path length needed for beam separation. However, the choice of an extraction array will depend not only on these factors but also on the overall optical design including: optical alignment, beam transfer systems, beam quality requirements and cost.

To illustrate this point, consider a linear array of beams passing through an amplifier as shown in Figure 2.1-7. Each beam is separated from its neighbor by θ so that the total array has a subtense of $N\theta$. If we wish to relay the beams through a subsequent amplifier, we would need to design relay optics capable of operating over a wide field without introducing unacceptable aberrations. It is unlikely that such a design would be possible with spherical surfaces and aspheres would need to be employed. Further, it is possible that a single optical element for the relay optics would be too large and individual elements for each beam would be required. In this case each optical element would need to work at a different field angle and would require a different optical design from all the other $N-1$ elements. Also, efforts must be made to equalize path lengths for each of the N pulses. Similar arguments can be made concerning square or rectangular arrays of beams.

Circular arrays, however, offer certain design advantages common to axisymmetric systems. In a circular array each of the N beams passes through the common pupil (in the amplifiers) at a fixed angle to the

Figure 2.1-7. Linear Beam Array Relaying. Each of the N beams requires its own relay optics which are unique for each of the beams (symmetry could reduce this to $N/2$ different designs).

center line so that each beam is working at the same field angle and therefore incurring the same off-axis aberrations. This results in a simplification of the optical system design and fabrication since each of the N beams will use identical optical elements and travel inherently equal path lengths. A system which operates at a single off-axis field angle such as this is referred to as a "ring field" design, and because of its advantages, in terms of the overall optical design, has been used throughout this conceptual study program.

Beam Separation

There are requirements for interstage and final spatial separation of the N beams which traverse each amplifier (module). Interstage separation (which we shall call preseparation) is needed to control the angular field of the N beam array within the amplifiers while final separation is needed for pulse compression by stacking. To understand why we need preseparation, consider the fact that the optical train has an inherent spatial magnification of nominally 40. This is the ratio of the diameter of the final amplifier aperture to that of the master oscillator. If we choose a field angle for the final amplifier to control ASE and parasitics and also achieve reasonable final separation lengths, we see that the angles required in the preceding stages are magnified to a level that causes both fill factor and optical quality problems. This is a consequence of the "Lagrange Invariant," that is, the product of the aperture and field angle remains constant throughout the system.

"Preseparation" is the term used to describe how we can avoid the constraints of the Lagrange Invariant. A preseparator is shown in Figure 2.1-8. The individual beams will separate at some distance from the first pupil. This distance depends on their diameter and angular separation. This separation distance can be shortened by bringing the beams to an image plane (focus) as described in Section 2.1.2. Flux damage limits will set a minimum dimension on the beam and we therefore locate the individual "rhomboid" mirrors accordingly. The "rhomboid" is actually a pair of slightly nonparallel flat mirrors

Figure 2.1-8. Pre-separation of Beams to Control Angular Field. Note that the beams can be magnified in angle and size by this approach.

which transfer the image location to a radial position such that we obtain angle (field) magnification. Note that this permits independent control of both spatial and angular magnification. In a circular array system each of the N beams requires a separate "rhomboid" to properly direct the beam to the next pupil. Preseparators are an important feature of this system concept.

The requirement for final separation is more obvious since we must ultimately delay the N beams by different amounts prior to stacking. We shall address these issues in the context of circular (axisymmetric) arrays due to their associated design advantages.

To maintain moderate cone angles throughout the optical train while the beam is repeatedly magnified, we have found that it is necessary to separate the beams prior to the separation required for the delay lines. In the region in which the beams are separated, their spatial packing may be restructured to adapt to requirements further along the optical train. One constraint that will be better met with a more compact beam arrangement than a conical array is the need for manageable separation distances for the high power beams. In the previous section we contrasted linear, square and circular (conical) arrays from the point of view of volume extraction and beam path length required for beam separation. Here we consider several variations of the conical array.

The beam packings shown in Figure 2.1-9 (a,b,c) are conical, conically nested, and a 180 degree sub-arc of a conically nested array. The distance to separation for each will be computed. In all three cases the beams on the perimeter will be assumed to be diverging at the same half-cone angle. This angle will be called θ . Numerical examples will be done assuming θ is one half degree (0.0087 rad).

For the case of the conical array, the requirement on the array radius, R_1 , is

$$2\pi(R_1 - d/2) \cong Nd$$

where N is the number of beamlines and d is the individual square beam

Figure 2.1-9. Beam Packing Arrays that are (a) Conical, (b) Nested Conical, and (c) a 180° Sub-arc of Nested Conical Beam Distributions. Each array may actually be of beams with a square rather than circular cross-section. The three arrays shown all contain approximately the same number of beams.

cross section diagonal. The beam area for each separated beam is $d^2/2$. The R_1 is therefore:

$$R_1 = (d/2)(N/\pi + 1)$$

For the case of a nested array of conical beam patterns the number of beams along the i -th conical arc, $i = 0, 1, 2, \dots$, are $2\pi r_i/d$ where r_i is the radius of the i -th arc. If the beams are packed as closely as possible without overlap, we will find that $r_i = (r_0 + id)$ where r_0 is the radius of the innermost arc. If the quantity β is defined as $\beta = r_0/d$ then $r_i = d(\beta + i)$. The total number of beams, N , is the sum over i of the number of beams, N_i , at each radius r_i .

$$N = \sum_{i=0}^n N_i = \sum_{i=0}^n (2\pi r_i/d) = \sum_{i=0}^n 2\pi(\beta + i) = 2\pi\beta(n+1) + \pi n(n+1).$$

This quadratic equation can be solved for n to get

$$n = -(1/2 + \beta) + 1/2 ((1+2\beta)^2 + 4(n/\pi - 2\beta))^{1/2}$$

Using this result the radius of the whole beam array can be computed to be $R_2 = d(\beta + n + 1/2)$ where r_0 , the radius of the innermost arc, is a design choice. Note that n must be integral, for example if n is computed to be 2.4, then a value of $n = 3$ must be used.

A variation of the nested array of conical beams is shown in Figure 2.1-9c. Here only half the angular spread of beams is utilized. This array will facilitate double pass amplification of the beam in the final amplifier because a symmetric flip of the beam about the optic axis will bring the beam out of its own light. Such a beam array will have radius R_3 where R_3 is computed in the same manner as R_2 , but with N replaced by $2N$:

$$\beta = r_0/d, n = -(1/2 + \beta) + 1/2 ((1 + 2\beta)^2 + 4(2N/\pi - 2\beta))^{1/2}$$

and

$$R_3 = d(\beta + (n + 1/2)).$$

The three beam packing concepts just discussed are shown in more detail in Figure 2.1-10. The beam packing along nested arrays will require that there be wasted space between beam lines. A possible tightly packed scheme is shown in Figure 2.1-11. The radius of this

Figure 2.1-10. Conical, Nested Conical and 180° Sub-arc of a Nested Conical Array. Packing must be ample enough to prevent any overlap anywhere, hence incomplete utilization of the space.

.. Figure 2.1-11. A Tightly Packed Array of Beams Contained Within a Half Circle, Radius R_4 , and the Radii of a Conical Distribution (R_1), and a 180° Sub-arc of a Nested Conical Distribution (R_3). All are for beam arrays with approximately 70 members.

beam array R_4 is determined by

$$(\pi(R_4 - d/2)^2/2)/(d^2/2) = N \text{ hence, } (R_4 - d/2)^2 = Nd^2/\pi \text{ so that}$$

$$R_4 = d(1/2 + N/\pi)^{1/2}.$$

In all cases the individual separated beam diameters are determined by the beam energies and the flux damage limits. If ϵ is the flux limit for a 10 nsec pulse and E is the total energy of the beams, then $d^2/2 = E/N\epsilon$ or solving, $d = (2E/N\epsilon)^{1/2}$. The length to separation, ℓ , is simply $\ell = R/\theta$, where R is the radius of the beam array.

For an example, let us consider two systems and calculate ℓ and R for each. We will assume $c = 2\text{J/cm}^2$ and $\beta = 1(r_0 = d)$ for the nested arrays. In this packing, only the centermost beam line is vacant.

		50 Beam 50kJ
Conical	R_1	2.62m
	ℓ_1	300m
nested conical	R_2	1.40m
	ℓ_2	160m
180° nested conical	R_3	2.02m
	ℓ_3	232m
closest pack	R_4	1.39m
	ℓ_4	159m
		$d = 31\text{cm}$

The ultimate choice of beam array depends on the cost and performance associated with each configuration. It is clear that the shorter the separation length, the easier it will be to maintain alignment. The total volume of the separation path must be evacuated to prevent degradation due to turbulence and thermal gradients. By minimizing the radius of the separated array, we reduce the volume to be evacuated. This will favorably affect cost and performance. For these reasons, one should choose nested circular arrays. Nested semicircular arrays,

however, compare favorably since they allow for greater fill factors in the amplifiers and lower aberration due to tilted relay mirrors.

By using a nested circular array, we must give up some of the advantages of a simple circular array; for example, the optical system must work over a finite field of view and this will set limits on the working f-numbers of the optics so as to keep off-axis aberrations (astigmatism) to acceptable levels. Thus, one must weigh the advantages of shorter path lengths to beam separation versus the disadvantage of differing amounts of phase aberration for each nested ring of beams. Further details on the effects of beam array configuration on system layout and beam quality will be discussed in Section 2.5.1.

2.1.4 Alignment

The requirements for alignment can be strongly influenced by the optical layout and design. Strategic placement of critical subsystems and components to minimize relative motion can result in a significant reduction in the alignment sensitivity and the need for active control. This philosophy has been followed in the optical system concept which is discussed in detail in Section 2.5.

2.1.5 Encoding

The front end in the angular multiplexing concept is responsible for delivering N pulses of duration τ seconds that are sequentially spaced in time to form a "long" pulse lasting $N\tau$ seconds. Each of the sequentially spaced N beams is individually distinguishable (separable) by virtue of its angular encoding that is also supplied by the front end. It has been shown that conical (circular) arrays have a great advantage over other possible geometries in terms of the overall optical design. Thus, we require the angular encoding, produced in the front end, to be compatible with the conical-axisymmetric design guidelines. There are a variety of encoding concepts that will accomplish this goal and for the purpose of discussion, we describe a generic encoder that will not only produce conical arrays but any array geometry that one wants to consider.

The generic encoder concept is shown in Figure 2.1-12. This concept uses beam splitters to generate new beams from a given beam line. Consider the first amplifier; it is extracted by four separate pulses (beam lines) of length τ and sequentially spaced in time. These pulses enter from the left and emerge amplified in intensity to the right. Each of the four initial beams is then split into four new beams, producing 16 total, that are now directed into the next amplifier by a set of 16 mirrors arranged on a ring as shown in the figure. This process is repeated another time to generate 64 beams. The final 64 beams can be arranged in a single conical array or nested array by simply choosing the mirror locations. The needed delays between adjacent beams are generated by the fixed spacings between beam splitters.

Figure 2.1-13 shows a block diagram of a complete encoder based on this concept. The four initial beams are arranged in a circular array as shown in the bottom left corner of the figure. The beam denoted as (1) is split into four beams and directed into the 16 beam circular array at the positions 1A, 1B, 1C, and 1D. By arranging the beams in this way, we have 6 meters between beam splitters giving a 40 nsec delay. The output from the third amplifier will be a common aperture which can be imaged onto the first main preamplifier of the optical train.

This generic encoder illustrates one possible technique for the input pulse train for a conical angular multiplexing scheme. For the remainder of this work, we treat the encoder as an exit pupil from which the desired extraction array is generated and direct our attention to the optical train leading through the preamplifiers, the final amplifier, and the final beam stacking.

2.1.6 Pupil Relaying and Spatial Filtering

Each amplifier in the optical train represents a common aperture through which each of the N multiplexed beams must pass. Beam transfer through such a series of apertures can be done without relay imaging, but there are particular advantages to be gained by using imaging relay optics. To illustrate this point, consider the optical schematic shown

Figure 2.1 -12. Generic Encoder Concept

Figure 2.1-13. Generic Erncoder Concept System Block Diagram

in Figure 2.1-14. This system is designed to transfer all the laser radiation passing through Pupil A to Pupil B. The lenses ℓ_1 and ℓ_2 image Pupil A to Pupil B. If we place an alignment source (a point) at P, its image will appear at Q. We can place a position-sensitive detector at Q to sense this radiation. In practice, there will be intervening optics between amplifiers which can introduce misalignments. An imaging relay system enables us to sense such misalignments more conveniently. Since the collimated light leaving Pupil A goes through an intermediate focus in the image plane, we can locate a spatial filter at this plane to control transmitted beam quality. (In practice, this region must be evacuated to prevent optical breakdown.) The degree of spatial filtering needed is determined by such factors as amplifier medium homogeneity and other sources of optical wavefront distortion. We shall show later how pupil relaying and spatial filtering is implemented in the system design for a complete module of 50 kJ.

2.1.7 Spherical Optics

Before turning from an overview of the optical concepts and techniques, it is worth pointing out that the linear arrays that use cylindrical focusing produced the shortest beam paths for beam separation and also extracted the largest fraction of the amplifier volume. However, this approach to angular multiplexing was dropped in favor of the conical array because of other compelling optical design constraints. In particular, the advantage of using an axisymmetric system and spherical optics in terms of alignment and cost was an important factor; also, the surface scatter from a cylindrical or aspheric surface is higher than for spherical surfaces at short wavelengths and this was another factor in terms of beam cross talk; and finally, even in a ring field system, each of the N beams is aberrated differently when cylindrical optics are used (refer to Section 2.3.5) making it necessary to correct each beam separately.

Figure 2.1-14. Pupil Relaying Schematic. Pupils A and B represent amplifier Apertures.

2.2 Optical Quality Budget

The optical quality requirements at the target are driven by the requirements for uniformity of energy deposition. The target is to be illuminated simultaneously by an array of beams. The uniformity of energy deposition is determined by the intensity profiles of the individual beams (single beam line effects), and by how the beams superimpose at the target (multiple beam line effects), as well as by the polarization and incidence angle dependent coupling of energy to the target.

2.2.1 Single Beam Line Effects

The focal spot size for a single beam must be less than the target size in order to efficiently utilize the laser output. The implications of this simple requirement can be appreciated by considering the possible design parameters for a 1 MJ system. We assume a target size of 150 μm and a standoff to target distance, R , of 25 M. The beam spot size at the target will be

$$\text{spot size} = m \left[\frac{2\lambda R}{D_b} \right]$$

where D_b is the beam aperture and m is the ratio of the actual spot size diameter to the diffraction spot diameter. To efficiently illuminate the target, this spot size must be less than the target diameter.

$$\frac{D_b}{m} \geq \frac{2\lambda R}{(\text{Pellet Diam})} = \frac{2(0.25 \times 10^{-4})(2500)}{150 \times 10^{-4}} = 8.3$$

Thus, if the beam quality is diffraction limited, $m = 1$, the beam aperture at the target chamber wall must be ≥ 8.3 cm.

The beam size at the target chamber will depend on the demagnification of the amplifier beam size during final beam separation, the magnification of the beam (if any) during transfer to the target chamber window, the maximum size of the chamber window and possible window segmentation at the amplifier. If the amplifier window is segmented, the beams from each segment may add coherently or incoherently at the target depending on the optical quality of the window sections.

To get an idea as to how these alternatives shape or limit the angular multiplexing design, we reconsider the 1 MJ system described above and include the possible segmentation of the amplifier window. If the amplifier window has an area D_w^2 and is segmented into an $n \times n$ array, we have $n^2 D_s^2 = D_w^2$ where D_s is the segment dimension. The total system energy is equal to the individual PAM energy times the number, M , of PAMs; $E_{TOTAL} = M E_{PAM}$. In addition, we impose damage thresholds on the amplifier window of 5 J/cm^2 for the long, $\sim 500 \text{ nsec}$, run time and 2 J/cm^2 at the target chamber window for the short, $\sim 10 \text{ nsec}$, exposure time of a single beam. Further, a constraint on the total target chamber area allocated to illumination optics is set at 0.4π steradians. These constraints are summarized in the following four equations

$$\begin{aligned} \text{DAMAGE:} \quad E_{PAM} &\leq (2 \text{ J/cm}^2) D_b^2 N \\ E_{PAM} &\leq (5 \text{ J/cm}^2) D_s^2 n^2 \end{aligned}$$

where N is the number of multiplexed beams per PAM.

$$10\% \text{ SOLID ANGLE LIMIT: } M N D_b^2 \leq 0.4\pi R^2$$

$$\text{FOCAL SPOT SIZE LIMIT: } \text{spot diam.} \geq m \left[\frac{2\lambda R}{(D_b/n)} \right]$$

where m is the ratio of the actual spot size to the diffraction-limited spot size.

$$\text{APERTURE LIMIT: } D_b \text{ and } D_s \leq 50 \text{ cm.}$$

In the expression for the focal spot size limit, the factor D_b/n is the width of the subaperture at the target chamber window; in this analysis we have assumed that the intensity from each subaperture adds incoherently at the target. If optical tolerances can be held between window segments, the full aperture will add coherently at the target and to examine this case, one would set $n = 1$.

Figure 2.2-1 shows how these various constraints limit the size (energy) of the PAMs. The two solid diagonal lines show bounds imposed by the damage threshold and the solid angle constraint for the 1 MJ system. The two broken diagonal lines show upper bounds on the PAM energy set by the optical quality for incoherent addition of the subaperture while the vertical broken lines, to the left, indicate the lower bound on D_b set by the optical quality when a single large aperture is used or when the intensity from each of the subapertures can be summed coherently. The shaded region in the figure indicates the region of operating space accessible to a PAM module when the optical quality is at least 2 times the diffraction limit. The interesting feature of plots of this kind is that they set upper bounds on the PAM energy based on optical constraints alone.

The optical aberrations in a beam line are due to aberrations, inherent in the optical design, figure errors on optical elements, and the effects of propagation on beam quality. The figure errors and propagation effects are both potentially dependent on gas and element heating and on beam intensity. Aberrations with high spatial frequencies, such as those due to small scale turbulence, may add statistically.

Figure 2.2-1. PAM Energy Constraints Set by Optical Quality, Damage Limitations, Target Chamber Geometry and Maximum Optical Window Size for a 1 MJ System.

Aberrations of lower spatial frequency, such as those characterized by lower order Zernike coefficients, add deterministically. Position and tilt in the wavefront can be compensated in the first order optical alignment. Low order aberrations can be kept to a minimum in design or with additional active optical elements. High order aberrations can be reduced by spatially filtering the beams at image planes.

Optical Elements

The presence of large aberrations will affect the beam quality or encircled energy in the far field. For a system which can be a few times diffraction limited (we found that $m \leq 2$ for the case shown in Figure 2.2-1), the figure tolerances are consequently difficult to establish. In the limit of geometric optics there is no more spreading due to aberrations for short wavelengths than for long wavelengths. For a system several times diffraction limited the geometric optics approach becomes useful. For the KrF system the standoff from target will be tens of meters and the f-number will be large (>70). This will cause the optical figure requirements on the elements to be finer for the KrF system than the CO_2 or glass laser systems, roughly in inverse proportion to the f-number. However, the CO_2 and glass laser systems are relatively close to diffraction limited. The optical surface quality requirements on the elements of the KrF system will be more stringent than for the CO_2 or glass systems, but may be reduced through the use of tight spatial filters or adaptive optical elements, the latter at considerably higher cost.

Beam Propagation (Beam Steering Errors)

Propagation of the beams through beam ducts which are not fully evacuated is a source of beam deflection and aberrations. Beam steering will occur due to index gradients caused by thermal or pressure gradients (turbulence). Thermally induced gradients in density will cause variations

in the optical path length. A temperature field with variations of high spatial frequency will induce high order aberrations in a beam. This will result in defocusing of the beam and an increased far field spot size.

Beam propagation in a medium with a thermal gradient is shown in Figure 2.2-2. As a beam of diameter D travels a distance dx through the gas at a distance x from the plane of the defining aperture, the beam is deviated by an angle $d\theta$.

Figure 2.2-2. Beam Steering Caused by Thermal Gradients.

The angle θ between the wave vector and the x -axis can be seen to increase linearly with x by the following argument. The upper and lower marginal rays of the beam travel through areas which differ in index of refraction by Δn . In an interval during which the upper marginal ray travels dx the lower marginal ray travels an extra distance of $(\Delta n)dx/n$ (for $\Delta n/n \ll 1$). As the beam travels a distance dx , it is deflected in angle by an amount $d\theta$ given by

$$d\theta = \frac{\Delta n}{n} \frac{dx}{D} ; \quad (2.2-1)$$

therefore,

$$\theta = \left(\frac{\Delta n}{n} \right) \frac{x}{D} \quad (2.2-2)$$

The y coordinate of the principal ray, which was initially directed along the x-axis, is determined by integrating the equation $dy = \theta(x)dx$:

$$y = \int_0^x \frac{\Delta n}{n} \frac{x'}{D} dx' = \frac{\Delta n}{2nD} x^2. \quad (2.2-3)$$

A thermal gradient will induce an index gradient, so that

$$\theta = \frac{x}{n} \frac{\Delta T}{D} \frac{dn}{dT} \quad (2.2-4)$$

where x is the length of the propagation path

n is the index of the gas

$\Delta T/D$ is the thermal gradient in $^{\circ}\text{C}/\text{m}$

Some representative values for the optical properties of gases are given below. The data is for gases at atmospheric pressures (STP) at .250 μm wavelength.

Gas	$n-1$	ρ (gm/cc)	$-d\rho/dT$	dn/dT	Reference
Argon	3.0×10^{-4}	1.65×10^{-3}	6×10^{-6}	1.1×10^{-6}	2.2-1
Helium	0.36×10^{-4}	1.67×10^{-4}	1×10^{-5}	2.2×10^{-6}	2.2-2
Nitrogen	3.2×10^{-4}	1.19×10^{-3}	2×10^{-6}	0.52×10^{-6}	2.2-3
Dry Air	3.0×10^{-4}			1×10^{-6}	2.2-4

For a beam which is 1 m square and propagating over a 100m path in a duct with a $1^{\circ}\text{C}/\text{m}$ lateral thermal gradient, the angle over which the beam is steered is 100 (dn/dT). For the gases above the steering is

Argon	110 $\mu\text{rad}/(^{\circ}\text{C}/\text{m})$
Helium	220 $\mu\text{rad}/(^{\circ}\text{C}/\text{m})$
Nitrogen	54 $\mu\text{rad}/(^{\circ}\text{C}/\text{m})$
Dry Air	100 $\mu\text{rad}/(^{\circ}\text{C}/\text{m})$

To be able to point to a target that is D_T in diameter within a fraction, η , of its diameter from a stand-off of L it is necessary that

$$|\theta| \leq \eta D_T / L$$

Suppose $D_T = 300 \text{ m}\mu$

$$L = 30 \text{ m}$$

$$\eta = 0.1$$

Then $|\theta| \leq 1 \text{ }\mu\text{rad}$

It is apparent that long beam paths must be conditioned to minimize thermal gradients. This will involve both temperature control and reduction in gas pressure. The beam steering will be linear in the density. If 1°C/m gradients are to be tolerated, the beam paths should be kept at pressures below .01 atm. It may not be adequate to use a low pressure duct of clean dry air because of absorption; the absorption coefficients for N_2 , O_2 , and O_3 at 250 nm and 1 atm are

$$\alpha_{\text{O}_2} = 2 \times 10^{-5} \text{ cm}^{-1} \quad (2.2-5)$$

$$\alpha_{\text{O}_3} = 3 \times 10^2 \text{ cm}^{-1} \quad (2.2-6)$$

$$\alpha_{\text{N}_2} < 1 \times 10^{-7} \text{ cm}^{-1} \quad (2.2-7)$$

Ozone would not normally be present, but may be created after absorption of UV radiation by O_2 . It would be a more serious problem in a repetitively pulsed system. For dry air the absorption due to N_2 is negligible in comparison to that due to O_2 . For air which is about 20% O_2 the absorption over 100 meters is

$$\begin{aligned} \frac{\Delta I}{I} &= 1 - \exp [-0.2 \alpha_{\text{O}_2} R(\text{cm})] \\ &= 0.039 \end{aligned} \quad (2.2-8)$$

A 100 meter beam path of dry air will have almost 4% absorption due solely to O_2 . Pure nitrogen, however, has over two orders of magnitude lower absorption and appears to be acceptable for low loss beam ducts.

Beam Propagation (Effects of Turbulence)

The effects of turbulence on KrF laser beam propagation will be discussed next. The work of Davis^(2.2-6) was used extensively in formulating this discussion. The effects of turbulence depend on the relative size of the beam diameter, D , and the characteristic scale, ℓ , of the turbulence.

For $D/\ell \ll 1$ The entire beam is refracted and the displacement is Rayleigh distributed for isotopic turbulence.

For $D/\ell \approx 1$ The beam is focused or defocused.

For $D/\ell \gg 1$ Parts of the beam are independently focused or defocused, resulting in spreading and distortion in the far field.

Over optical paths within a few hundred meters of the earth's surface turbulent eddies range from several mm to tens of meters in scale.

For subsequent use define

D = beam diameter

ℓ = spatial scale of turbulence

R = range of propagation through turbulence

C_n = structure constant for refractive index fluctuations

λ = wavelength

k = wavenumber

If $\sqrt{\lambda R}$ is small with respect to all the turbulent scales present then the fluctuation in the logarithmic level of intensity is given by

$$\chi_I^2 = \langle (\log(I/I_0))^2 \rangle = 1.2 C_n^2 k^{7/6} R^{11/6} \quad (2.2-9)$$

where I is the intensity at a receiver relative to a source intensity of I_0 . Like the intensity, the phase of the beam at the receiver plane is aberrated in a normal distribution due to the turbulence. For points separated by a distance ρ the rms phase fluctuation is

$$\sigma_\phi(\rho) = 1.7 C_n k \sqrt{R} \rho^{5/6} \quad (2.2-10)$$

If $\ell \gg D$ (the scale of turbulence is large) then the wavefront is steered by an angle $\sigma_\theta = \sigma_\phi / k\rho$, but the wavefront is otherwise undistorted. If $\ell \ll D$ (the scale of turbulence is small) the wavefront is distorted with the phase across the beam becoming uncorrelated for sufficiently large ρ . This is the case of beam spreading. The lateral coherence length can be defined as ρ_0 satisfying

$$\sigma_\phi(\rho_0) = \pi$$

If the lateral coherence length is much less than the beam diameter ($\rho_0 \ll D$), the beam appears spatially incoherent at the receiver.

The effects of turbulence on a KrF laser beam propagating through a long beam path are calculated below using the following data

$$\lambda = 2.47 \times 10^{-7} \text{ m}$$

$$D = 1.5 \text{ m}$$

$$R = 100 \text{ m}$$

$$C_n = 8 \times 10^{-9} \text{ m}^{-1/3} \quad (\text{weak turbulence})$$

The structure constants, C_n , for intermediate and strong turbulence are $4 \times 10^{-8} \text{ m}^{-1/3}$ and $5 \times 10^{-7} \text{ m}^{-1/3}$ respectively.

The logarithmic intensity fluctuations are

$$X_I^2 = 1.6 \times 10^{-4}$$

The lateral phase coherence is

$$\rho_0 = 0.23 \left(\frac{\lambda}{C_n^2 R} \right)^{3/5} = 0.89 \quad (2.2-11)$$

The rms phase fluctuations are

$$\sigma_\phi(\rho) = 3.47 \rho^{5/6} \quad (2.2-12)$$

$$\sigma_\phi(1.5) = 1.55\pi \quad (2.2-13)$$

The rms steering angle is

$$\sigma_\theta(1.5) = 1.3 \times 10^{-7} \text{ rad} \quad (2.2-14)$$

All of these effects can be mitigated by reducing the pressure in the beam path. The reduction in pressure to below 1/100 atm, as suggested

to reduce thermal effects, would be more than is needed to make the effects due to turbulence negligible. The effects due to thermal gradients are the main source of beam path aberrations.

Nonuniform Gain Medium Effects on Optical Quality

A nonuniform gain medium can cause refraction of the optical beam and is an important consideration in the analysis of cross talk and possible pre-pulse problems. This particular question is addressed in Section 2.4 on pre-pulse suppression; in this section we consider the effects of a nonuniform laser gain medium on the optical beam phase front.

The impact of the maximum beam phase distortion is two fold: first, there is a minimum guide field required to optimize not only the transmission of the hibachi but also the pumping uniformity to produce good beam quality; second, if segmented cathodes are used to reduce the guide field requirements we must understand if weakly pumped regions of the amplifier, as a result of segmentation, lead to unacceptable beam quality.

Nonuniform pumping will lead to nonuniform free electron densities as well as a nonuniform density distribution of absorbing species. The possible phase front distortion caused by both of these components in the gain medium will now be discussed.

Electron Density: The index of refraction of a pure electron plasma (no external magnetic field present) is given by

$$\mu^2 = 1 - \frac{(\omega_p/\omega)^2}{1 + (\nu_c/\omega)^2} - i \frac{(\nu_c/\omega)(\omega_p/\omega)^2}{1 + (\nu_c/\omega)^2}$$

where ν_c is the collision frequency, ω is the frequency of the incident radiation and

$$\omega_p = \frac{4\pi n_e e^2}{m_e} = 5.6 \times 10^4 n_e^{1/2}$$

is the plasma frequency. With $\nu_c/\omega \ll 1$ we have

$$\mu^2 = 1 - \left(\frac{\omega_p}{\omega} \right)^2$$

The resulting phase shift in numbers of fringes over a path length L is

$$N = \frac{1}{\lambda} \int_0^L (\mu - 1) d\ell$$

Since $n_e \approx 10^{15} \text{ cm}^{-3}$ we find $\omega_p = 1.8 \times 10^{12}$ which is much less than $\omega = 7.5 \times 10^{15}$. Thus $\mu - 1$ becomes

$$\mu - 1 \approx 4.41 \times 10^{-14} n_e (\text{cm}^{-3}) \lambda^2 (\text{cm}) ,$$

and the phase shift in fringes is then

$$N = 1.1 \times 10^{-18} n_e (\text{cm}^{-3}) L (\text{cm}) .$$

with $L = 100 \text{ cm}$ and $n_e = 10^{15} \text{ cm}^{-3}$. The maximum possible phase shift due to electron density nonuniformities is 0.11. Any actual phase shift due to this effect will be much smaller because the nonuniformities in electron density will be spread over a much shorter path than L , and because the density fluctuations will be much less than 10^{15} cm^{-3} . Consequently, we conclude that electron density fluctuations are not a serious beam quality problem.

Absorbing Species: In the presence of absorbers, an electromagnetic wave experiences a susceptibility that is imaginary and that can be described as

$$\chi''(\omega) = \chi'(\omega) - i\chi''(\omega)$$

where

$$\chi'(\omega) = \frac{Ne^2}{8\pi^2 m \epsilon_0} \cdot \frac{(\nu_0 - \nu)/\nu_0}{\left[\left(\frac{\Delta\nu}{2}\right)^2 + (\nu - \nu_0)^2\right]}$$

and

$$\chi''(\omega) = \frac{Ne^2}{16\pi^2 m \epsilon_0} \cdot \frac{(\Delta\nu/\nu_0)}{\left(\frac{\Delta\nu}{2}\right)^2 + (\nu - \nu_0)^2}$$

The imaginary part leads to the absorption while the real part produces a phase shift in the propagating beam. The susceptibility can be related to the propagation vector k as

$$k = k_0 \left[1 + \frac{\chi'(\omega)}{2\mu^2} \right] - i \frac{k_0 \chi''(\omega)}{2\mu^2}$$

where k_0 is the free space propagation vector.

The index of refraction is written as

$$\mu = 1 + \frac{\chi'(\omega)}{2\mu^2}$$

In order to compute an upper bound on $\mu-1$ we take $\nu - \nu_0 = (\Delta\nu/2)$; the function $\chi'(\omega)$ has a peak at this frequency. Substituting this value and taking $\mu^2=1$ one finds

$$\mu - 1 \approx \frac{Ne^2}{16\pi^2 m \epsilon_0 \nu_0 \Delta\nu}$$

Using $N=10^{15} \text{ cm}^{-3}$, $\nu_0 \approx 1 \times 10^{15}$ and $\Delta\nu = 5 \times 10^{14}$ the index of refraction is determined as

$$\mu - 1 \approx 10^{-7}.$$

The maximum phase shift in fringes is then

$$N = (\mu - 1) L / \lambda ;$$

with $L=100 \text{ cm}$ and $\lambda=.25 \times 10^{-4}$ the maximum phase shift possible for a double pass amplifier is ≈ 1 fringe.

From the KrF kinetics code, density variations in absorber species can be determined by considering different pumping rates. For example, the absorbers F^- , Kr_2F^* and F_2 all show density differences of 10^{13} to 10^{15} cm^{-3} respectively when the pumping rate changes from 500 to 700 kw/cc. Since we expect to hold the pumping uniformity to within 5-10 percent it is clear that using absorber densities of 10^{15} cm^{-3} produces a high upper bound in the phase shift. Further, even if the nonuniformity in density were as high as 10^{15} cm^{-3} it would not exist over the entire amplifier: absorber density fluctuation caused by segmenting the e-gun or due to nonuniform transmission through the hibachi is not expected to exist over more than 10-20 percent of the amplifier volume. Finally, the calculation carried out above for $\mu-1$ essentially assumes an oscillator strength of unity and computes $\chi'(\omega)$ at its maximum value. Considering all these conditions it seems very unlikely that nonuniform absorber production could lead to phase errors of more than $\lambda/10$. If on further analysis the design of the e-gun, guide field or hibachi should dictate very different conditions than we have considered then this source of phase aberration will need to be re-evaluated. For the present it does not appear to be a serious problem.

2.2.2 Multiple Beam Irradiance Effects

Since we are treating a multiplexed amplifier chain where each of the N multiplexed beams traverse similar or common optical paths, we need to consider the consequences on target irradiance. For these discussions, we shall treat the case where the N beams form an array which is brought to focus as a single beam line (assumed to be of many separate beam lines from separate amplifier modules). We are concerned with resultant irradiance of the pellet under conditions of incoherent addition of the N multiplexed beams; we assume incoherent addition due to the long path delays between beams. Since the baseline design uses segmented material windows for the final amplifier module, we have analyzed the effects of window optical thickness differences between segments. This analysis must also be divided into two cases: (1) where the subaperture segments of the beam add coherently as one large aperture, and (2) where each subaperture beam adds incoherently with the adjacent subaperture beams. The first case corresponds to window errors that are small compared to the coherence length of the laser. The second case corresponds to much larger path length differences between subapertures.

Incoherent Addition of Subapertures

Let us first address the focusing length constraints imposed by the focal spot size requirements under conditions of incoherent addition of subapertures at the pellet. Incoherent addition is desirable since it allows for much less stringent tolerances on window segment fabrication. We wish to consider the resultant irradiance (focal spot size) produced by (a) a single beam assuming a segmented window at the final amplifier,

and (b) the summation of N separate beams at the pellet surface. The optical schematic for a single beam line can be represented as shown in Figure 2.2-3. A focusing element is included in the final amplifier to achieve beam separation in a shorter distance than if the beams were full aperture and collimated. The final amplifier window is treated as an n by n array of square segments with dimension D/n on a side. This beam size is reduced from D to h at the beam separation plane. We assume that the target chamber window and focusing optics (as necessary) will be single (nonsegmented) elements. At the final focusing element the beam is therefore an n by n array of square subaperture beams each with dimension h/n on a side. The center-to-center spacing of the beams, s , is also equal to h/n . Figure 2.2-4 shows in a simplified way the geometric focusing of the beams to a pellet (assumed to be a sphere). Each of the subaperture beams ideally comes to diffraction limited focus of nominal dimension $4f\lambda n/h$ at the beam waist (focus). At a distance z ahead of focus (the tangent plane to the pellet) the center-to-center spacing of the beam subapertures is $\delta = \frac{zs}{f} = \frac{zh}{fn}$. The beams will actually overlap at the pellet because each beam diameter is larger than δ due to diffractive effects. Using Gaussian beam relations, the actual individual beam size (radius) will be given by

$$w^2(z) = w_0^2 \left(1 + \frac{z^2}{z_0^2} \right) \quad (2.2-15)$$

where $w_0 \approx \frac{f\lambda n}{h}$ and $z_0 = \frac{\pi w_0^2}{\lambda}$. The full beam size at the pellet tangent plane due to all n^2 beams is therefore $W = X\sqrt{2}$, where $X = [2w(z) + (n-1)\delta]$. The $\sqrt{2}$ factor accounts for the largest diagonal dimension since the spots form a square array.

Let us extend this calculation to include N such multiplexed beams from a single module which, for simplicity, form an m by m array at the target chamber, each beam being identical to the case above. In this case, the spot size at the pellet plane is $W_m = \sqrt{2} [X + (m-1)\Delta]$ where $\Delta = zh/f$. (2.2-16)
In writing this Equation, we have assumed that each of the N multiplexed beams is brought to the same geometrical focal spot position defined

Figure 2.2-3. Schematic of Optical System for Separating, Delaying and Focusing to Target. All elements are shown as transmissive equivalents of actual elements.

Figure 2.2. -4. Subaperture Beam Irradiance of the Fusion Pellet. The beam elements are shown as circular for clarity but are actually square.

in Figure 2.2-4. At $z = 0$ it is possible to direct each of the N beams at different focal points to reduce the overall spot size at the pellet so that this analysis represents a worst case. We want W_m to be smaller than the pellet diameter, d , by some factor x where $x = \frac{W_m}{d} < 1.0$. Solving the above equations in the limit of $a \gg 1$, the defocus required to achieve a spot size $W_m = xd$ becomes

$$z = \frac{f}{ha} (\sqrt{2} xd - 4\omega_0) \quad (2.2-17)$$

where

$$a = 2 \left[\frac{(n-1)}{n} + (m-1) \right] \quad (2.2-18)$$

and either n and/or m are greater than unity. Since we require $z \geq 0$ we find that

$$xd \geq \sqrt{2} (2\omega_0) = \sqrt{2} \left(\frac{2f\lambda n}{h} \right)$$

or

$$\frac{h}{n} \geq \frac{2\sqrt{2}f\lambda}{xd} \quad (2.2-19)$$

When the equality sign is used, the focal spot is located at the pellet surface ($z=0$) and this expression simply says that the target diameter, dx , must be equal to the diffraction spot size from a single subaperture of width h/n . The subaperture size at the target chamber entrance pupil (h/n) is related to the subaperture size at the final amplifier output window by

$$D = Mh$$

where both D and h are limited by damage fluence of the optics at the corresponding pulse durations. At the final amplifier D is limited to

$$D^2 \geq \frac{E}{\epsilon_{th}(Nt)} \quad (2.2-20)$$

while at the separation plane (or target chamber window)

$$h^2 \geq \frac{E}{N\epsilon_{th}(t)}$$

The damage threshold fluence, ϵ_{th} , for pulse duration t can be assumed to scale as $t^{1/2}$. Therefore

$$\epsilon_{th}(Nt) = \sqrt{N}\epsilon_{th}(t)$$

and consequently

$$M = \frac{D}{h} = N^{1/4} \quad (2.2-21)$$

This leads to the requirement on the final amplifier window segment size D/n for a given focal spot size x_d which is

$$\frac{D}{n} \geq \frac{2\sqrt{2}\lambda fN^{1/4}}{x_d} \quad (2.2-22)$$

where N is the number of multiplexed pulses (compression ratio). If we treat D/n as a limiting parameter due to other constraints on homogeneity or pressure we can use the above relation to define a maximum $fN^{1/4}$ product (focal length X compression ratio^{1/4}) to achieve a given spot size at the pellet, where

$$fN^{1/4} \leq \frac{D}{n} \frac{x_d}{2\sqrt{2}\lambda} \quad (2.2-23)$$

This relation is plotted in Figure 2.2-5. For small spot sizes $\sim 200\mu\text{m}$, and practical window sizes $D/n \leq 50\text{cm}$, $fN^{1/4}$ must be less than 140 meters. Larger pellets and focal spot sizes permit larger focal lengths and compression ratios. Smaller pellets will require consideration of coherent addition of the subaperture beams, that is, the final amplifier window must be treated as a single coherent aperture.

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Figure 2.2-5. Maximum $f N^{1/4}$ Product for a Given Focal Spot Size, x_d , as a Function of Final Amplifier Window Element Size, D/n .

Coherent Addition of Subapertures

An analysis has been performed to evaluate the effects of segmented windows of unequal thickness on the pellet irradiance assuming coherent addition of the subaperture beams. For this analysis we chose a 4 X 4 array of windows, each 25cm square. The thickness error from nominal was treated as a Gaussian random variable with zero mean and a specified deviation. A TI-59 calculator was used to generate the random thickness error values. A uniform intensity and phase is assumed prior to the window. After passage through the window the beam is propagated 50m to the beam separation plane (where it is assumed to be 20cm square) and collimated for subsequent propagation to the target chamber (100m). The beam is then focused with a 300cm focal length lens. The following figures show the intensity and/or phase at the various planes in the system and at the focal plane. Before examining these plots we first derive expressions for pellet location and spot size as follows. Following the analysis given earlier for incoherent subaperture addition, we can derive a relationship for coherent subaperture addition. For coherent addition we find that $n = 1$ (ideal case) since the segments are of nominally equal thickness (errors $\ll$ coherence length). Therefore, $w_0 \approx f\lambda/h$, and $a = 2(m-1)$ for an $m \times m$ array of beams at the target chamber. The location of the pellet surface from focus is therefore given by

$$z = \frac{f}{ha} (\sqrt{2} \, x_d - 4 \, w_0) \quad (2.2-24)$$

or

$$z = \frac{f}{2h(m-1)} \left(\sqrt{2} \, x_d - \frac{4f\lambda}{h} \right)$$

where x_d = spot size of the $m \times m$ array of beams. For the case considered above, $h = 20\text{cm}$, $f = 300\text{cm}$, $m = 6$, $x_d = 150\mu\text{m}$, and $\lambda = 0.25\mu\text{m}$. For this case the Raleigh length $z_0 = \frac{\pi w_0^2}{\lambda} = 706.9\mu\text{m}$. Since the pellet diameter is $300\mu\text{m}$ (assumed) we see that we need to focus beyond the center $\sim 100\mu\text{m}$. Since these values are much less than z_0 we can safely assume the system to be focused at the pellet center for all practical purposes.

Figure 2.2-6 shows the starting wavefront phase with the unequal window thickness effects shown. Figures 2.2-7 (a) - (d) show the pellet irradiance due to a single beam with the standard deviation of window thickness errors ranging from 0 to 0.25 waves. These results show that coherent illumination with a single beam places extremely tight tolerances on the segmented window optical thickness. Similar results can be obtained if we treat each segment as a wedge (prism) where the angle is the random variable.

If we now consider a multiple beam array where each of the N multiplexed beams is passed through the same segmented window of a common final amplifier and then brought to focus the results are quite different. (Note that in this case, we are coherently summing the wavefront over the aperture, but then adding, incoherently, the intensity from each of the N multiplexed beams.) Using the same starting wavefront as above and again treating the window element thickness errors as a Gaussian random variable with the standard deviation as a parameter, we obtain the results shown in Figures 2.2-8 (a) and (b). Figure 2.2-8 (a) shows the resultant pellet irradiance with an ideal window while 2.2-8 (b) shows the results with a half-wave standard deviation in optical thickness (same scale dimensions). Note that $N = 30$ in this test case. This result is not nearly as severe as depicted above for single beam illumination. Note that peak irradiance has decreased approximately 50 percent due to window thickness errors. We conclude therefore that coherent addition of subaperture beams from a segmented window may be feasible if required for small spot sizes but only for conditions of multiple beam illumination (all N beams). These results were obtained with the aid of the Perkin-Elmer Physical Optics Propagation Code.

Figure 2.2-6. Starting Wavefront of the Passage through the Segmented Window.

(a)

(b)

Figure 2.2-7 (a) - (b). Pellet Irradiance Due to a Single Beam Passing through a 4 x 4 Segment Window with Thickness Errors Varying from Zero to $\lambda/4$ (Standard Deviation).

(c)

(d)

Figure 2.2-7 (c) - (d). Pellet Irradiance Due to a Single Beam Passing through a 4 x 4 Segment Window with Thickness Errors Varying from Zero to $\lambda/4$ (Standard Deviation).

Figure 2.2-8 (a) and (b). Pellet Irradiance Due to Multiple Beam Illumination. Each of the 30 beams has passed through a common segmented window with (a) equal thickness segments, and (b) $\lambda/2$ standard deviation of thickness.

2.3 Windows For the Power Amplifier

In this section, the issues associated with material windows and aerodynamic windows will be discussed. For material windows, the questions of material choice, window segmentation, window mounting and window heating due to absorption will be addressed in terms of the amplifier design and impact on optical quality. For nonmaterial windows, we consider the changes in the optical design that are necessary to accomodate such a window and the restrictions imposed on the alignment system.

2.3.1 Materials Choices

There are several considerations that govern the choice of optical materials for the transmissive elements of the KrF scaling module. The simplest requirement of transparency at 248 nm will be satisfied by many materials with band gap energies exceeding the photon energy $h\nu$ (approx. 5eV at 248 nm). There are, however, many other important optical and mechanical considerations. The optical considerations other than transparency at low intensity fall into two categories: optical fabrication and nonlinear optical behavior of the material.

The figure and surface roughness requirements needed for the UV optical system are difficult to achieve with well known and cooperative optical materials, let alone with exotic materials, and the figure must be stable in time so that the elements have an acceptable useful lifetime. The ability to fabricate the necessary elements within a reasonable budget is also one of the foremost considerations in choosing an optical material.

Optical behavior beyond first order absorption and index effects is also very important at high power levels and enters into material choices. In particular the third order nonlinear effects, two photon absorption and intensity dependent variations of the index of refraction should be considered. The imaginary part of the nonlinear susceptibility, $\chi^{(3)}(\omega)$, governs two photon absorption which gives rise to an increase in beam attenuation at high power. The real part of $\chi^{(3)}(\omega)$ governs intensity dependent index changes in the material which cause small differences in the optical path length resulting in self-focusing (or defocusing) which lead to beam breakup. Care in the choice of materials and in construction can minimize these nonlinear effects.

There are several standard optical materials that are transparent at low power in the UV, but large two photon absorption coefficients, β , eliminate many of them from high power use. The attenuation due to two photon absorption is given by

$$\frac{dI}{dz} = -\beta I^2 \quad (2.3-1)$$

It can be shown that small two photon absorption coefficients are characteristic of materials with energy band gaps greater than $2h\nu$ where ν is the light frequency.

Considering only absorption, the best window choices would appear to be LiF (Band gap energy 11.6 eV, $\beta < 1.0$ at $.266\mu\text{m}$), CaF_2 (Band gap energy 10.0 eV, $\beta < 2.0$ at $.266\mu\text{m}$) and SiO_2 (Band gap energy 7.8 eV, $\beta = 4.5 \times 10^{-12}$). There is evidence that CaF_2 will darken after exposure to UV light due to formation of color centers. This would make it an unacceptable window material. LiF is promising for the small windows because of its high band gap energy and low index of refraction, but is difficult to obtain in large sizes. Fused silica (SiO_2) is slightly more absorbant, but is currently the best choice for the large windows, at least from the point of view of absorption, availability of materials and amenability to standard optical techniques.

A high power laser beam propagating through the windows and lenses will induce changes in the index of refraction of the medium which depend on the intensity of the beam, generally increasing the index of refraction. If the intensity of the beam has a central peak (Gaussian) the index changes will cause overall self-focusing. A square intensity profile (top hat) will not experience this large scale self-focusing; however, unavoidable small variations in the intensity will lead to local self-focusing which causes greater intensity variations and ever-growing self-focusing. This results in a degradation in beam quality, lowering the amount of energy actually focused on the target.

The difficulties due to the nonlinear index of refraction can be estimated by calculating the nonlinear index. First, the index of

refraction will be written as:

$$n = n_0 + \Delta n \quad (2.3-2)$$

where n_0 is the linear index, and Δn represents the intensity dependent index. To second order Δn is commonly written as:

$$\Delta n = \gamma I$$

or

$$\Delta n = n_2 \langle E^2 \rangle. \quad (2.3-3)$$

The nonlinear index, n_2 , is related to $\chi^{(3)}$ by the following:

$$n_2 = \frac{12\pi}{n_0} \operatorname{Re} \chi_{1111}^{(3)}(-\omega, \omega, \omega, -\omega). \quad (2.3-4)$$

Boling, Glass, and Owyong^{2.3-2} have derived an expression for $\chi^{(3)}$ using a nonlinear Lorentz model.

$$\operatorname{Re} \chi_{1111}^{(3)} = \left(\frac{n^2(\omega) + 2}{3} \right)^2 \left(\frac{n^2(\omega) - 1}{4\pi} \right)^2 \frac{1}{M\omega_0} \quad (2.3-5)$$

where ω_0 is the resonant frequency of the medium.

The third order susceptibility is seen to be a function of the linear refractive index, n , which is itself a function of frequency. Since $n(\omega)$ increases with increasing ω for $\omega < \omega_0$, $\chi_{1111}^{(3)}$ will increase with frequency. Little data exists on the nonlinear index at .25 μm ; however, we can calculate the nonlinear index at .25 μm relative to that of 1.06 μm by using the formula of Boling, Glass, and Owyong and the expression for n_2 in terms of $\chi_{1111}^{(3)}$. From dispersion data for Corning Fused Silica (SiO_2) we see that $n_0 = 1.45$ at 1.06 μm and $n_0 \approx 1.51$ at .25 μm so,

$$\frac{n_2(.25\mu\text{m})}{n_2(1.06\mu\text{m})} = \frac{\frac{12\pi}{n(.25\mu\text{m})}}{\frac{12\pi}{n(1.06\mu\text{m})}} = \frac{\operatorname{Re} \chi_{1111}^{(3)}(.25\mu\text{m})}{\operatorname{Re} \chi_{1111}^{(3)}(1.06\mu\text{m})} \quad (2.3-6)$$

where

$$\frac{\text{Re } \chi_{1111}^{(3)} (.25\mu\text{m})}{\text{Re } \chi_{1111}^{(3)} (1.06\mu\text{m})} = \frac{\left(\frac{n_{.25}^2 + 2}{3}\right)^2 \left(\frac{n_{.25} - 1}{4\pi}\right)^2 \frac{1}{N\epsilon\omega_0}}{\left(\frac{n_{1.06}^2 + 2}{3}\right)^2 \left(\frac{n_{1.06} - 1}{4\pi}\right)^2 \frac{1}{N\epsilon\omega_0}} \approx 1.47$$

(2.3-7)

so

$$\frac{n_2 (.25\mu\text{m})}{n_2 (1.06\mu\text{m})} = 1.47 \frac{n_{1.06\mu\text{m}}}{n_{.25\mu\text{m}}} = 1.41 \quad (2.3-8)$$

The generated index change at .25 μm will be about 1.4 times that generated at 1.06 μm in SiO_2 .

A deeper understanding of the problem is given when considering the phase of the beam rather than the index. The additional phase change induced by the nonlinear index is given by

$$B = \frac{2\pi}{\lambda} \int_0^L \Delta n \, dz = \frac{2\pi}{\lambda} \int_0^L n_2 \langle E^2 \rangle \, dz \quad (2.3-9)$$

where L is the total path length through the material. The ratio of the B parameters for SiO_2 at 1.06 μm and 0.25 μm is,

$$\frac{B_{0.25\mu\text{m}}}{B_{1.06\mu\text{m}}} = 5.97 \quad (2.3-10)$$

This shows that, all else being equal, the nonlinear index will induce about 6 times more beam distortion per unit distance at 0.25 μm than at 1.06 μm . In addition, intensity variations induce greater intensity variations because of n_2 ; this cascading effect causes an exponential growth in intensity variations described by

$$\Delta I = \Delta I_0 \exp (2B) \quad (2.3-11)$$

where ΔI_0 is the original local intensity variation and ΔI is the final intensity variation. It is evident that nonlinear beam breakup would be a serious problem if the path length through the material were as long

as in the Shiva glass laser system. Because KrF is a gas laser there will naturally be less transmissive glass.

The solution to both of the nonlinear problems mentioned will be to limit the total path length through the material. This can be done by keeping the total number of transmissive optics as small as possible, and by using struts to support any large pressure windows. For constant pressure and window deflection, the required thickness of the window will be at least proportional to the free span of the window. By bracing the window, the thickness can be reduced significantly at a small cost of clear aperture. Reducing the thickness will help alleviate both the two photon absorption and nonlinear index problems. SiO_2 is a favorable choice due to its relatively high strength. LiF seems a poor choice due to its low intrinsic strength and tendency to cold flow. SiO_2 is currently the preferred material choice because it has acceptable UV optical properties, excellent optical fabrication properties and availability in large sizes. The recent experimental work of Deaton and Smith^{2,3-3} has confirmed the high damage limits of SiO_2 and its desirability as a window material in the UV. Resistance to fluorine attack of the windows may be an issue, but if the optics are coated, then the burden of protection will fall on the exterior layer of the coatings.

2.3.2 Window Segmentation

Window segmentation offers an option for avoiding large monolithic blanks which might otherwise be needed for the amplifier windows. The advantages in using window segments with reduced clear apertures are in cost, reliability and optical performance. The drawbacks include vignetting by the frame and possible increased complexity in alignment. A window of meter dimensions composed of half meter segments supported in a rigid frame utilizes window elements which are available within the realm of current technology. The use of a rigid support frame could allow the window thickness to be reduced, thereby alleviating difficulties due to nonlinear beam break-up in the windows. Also, if a single segment in a mosaic window is damaged, it may be replaced without the loss of

the whole window, giving increased reliability as compared to single large window.

Brewster Angle Windows

A geometric argument based on a simply supported window will be presented in this section showing that as an optically flat window is subject to pressure it will give aberration to the transmitted beam.

If the window is at nonnormal incidence, such as a Brewster angle window, bowing will cause the optical path length through the window to vary across its aperture as shown in Figure 2.3-1, and this is the source of aberration. Windows at normal incidence induce substantially less aberration. When a beam passes through a bowed window with parallel faces, the marginal rays deviate with respect to the on-axis principal ray by an amount β given by:

$$\beta = \frac{2(n-1) \sin \theta \Delta \theta}{n a \cos^3 \theta} \quad (2.3-12)$$

where n is the index of refraction and a is the aspect ratio of the window. The magnitude of the aberration in units of waves of optical path difference across the window (OPD) is

$$\text{OPD} \approx \frac{L\beta}{2\lambda} \quad (2.3-13)$$

Where L is the width of the aperture. If we insist that $(\text{OPD}) \ll 1$ for a Brewster angle window where $\theta = 56^\circ$ for $n = 1.5$, assuming the aspect ratio is 10 and the clear aperture is .5m, then

$$\Delta \theta \ll 3\mu \text{ radians} \quad (2.3-14)$$

This is a tolerance on window deflection. It can be greatly reduced if the window is at near-normal incidence, as can be seen from Equation (2.3-12).

Figure 2.3-1. A Brewster Angle Window Bowing Under Pressure Will Change the Optical Path Length Across the Window and Induce Aberrations.

Consequently, if the windows are to be immune to aberration, Brewster angle windows are not desirable. Rigidly clamped windows would perform somewhat better than the simply supported windows analyzed here, both at Brewster angles and at near-normal incidence. Coated windows at near-normal incidence are the favored alternative.

Segmented Windows and Tolerances on Wedge

The degree to which the window segments must be free of wedge, which may differ from one segment to the next, can be estimated by insisting that the beam is not aberrated appreciably at the target. Let the characteristic variation in wedge among the window segments be θ , the focal length to the target be f and magnification of the beam between the amplifier and the focusing optics be M ($M < 1$). The displacement at the target due to the variation in wedge is

$$\text{displacement} = (\theta f / M) \quad (2.3-15)$$

For the displacement to be acceptable it must be much less than the pellet diameter, d .

$$(\theta f / M) \ll d \quad (2.3-16)$$

For $M=1/5$, $f=25\text{m}$ and $d=1\text{mm}$

$$\theta \ll 8\mu \text{ radians} \quad (2.3-17)$$

If the pellet is an order of magnitude larger or smaller, the allowed variation in wedge between the window segments will also be larger or smaller accordingly. This tolerance in wedge is not exceedingly difficult to attain; the windows must have less than 6 waves of wedge in the visible over a 0.5 m aperture.

Structural Rigidity and Segmentation

Deflection of the window segments will induce aberrations. In a previous section, it was demonstrated that the windows would need to maintain flatness within a few microradians if they were at Brewster angle. However, windows at near-normal incidence will have at least an order of magnitude greater tolerance on their bowing, as is evident from Equation (2.3-12). In this section, an allowable tilt of 28μ rad. will be assumed, and the required number of segments, segment thickness, and frame design will be analyzed.

Assuming each segment has simply supported edges, strength considerations will limit the segment sizes and thicknesses. Using the theory presented in Timoshenko^{2.3-4} the required glass thickness is calculated. A maximum design stress in the glass is taken to be 100psi. The resultant expression for the window thickness, t , is

$$t = \frac{0.175 A}{n} \quad (2.3-18)$$

where A is the span of the full aperture and n is the number of segments per side ($n \times n$ array). This relationship is displayed in Figure 2.3-2 (a). In Figures 2.3-2 (b) and (c), the constraints imposed on the segment thickness for a less restrictive allowance on tilt (250μ rad) are given for simply supported and clamped windows respectively. The clamped windows have clearly superior properties. Windows in 4×4 segmented array forming a 1 meter aperture will have a required thickness which is approximately three times less than that for a single 1 meter window, and this is motivation to use segmented windows.

Deflection considerations also place constraints on the allowable segment sizes and thicknesses. The maximum deflection, δ , of a flat material is proportional to the thickness, t . Using the material properties of glass we find that

$$\delta = \frac{3.1 \times 10^{-6}}{t^3} \left[\frac{A}{n} \right]^4 \quad (\text{cm}) \quad (2.3-19)$$

Figure 2.3-2(a). Window Thickness Required for a Segmented Window Where the Maximum Design Stress is 1000 psi.

Figure 2.3-2(b). Window Thickness Requirements for Segmented Simply Supported Windows.

Figure 2.3-2(c). Window Thickness Requirements for Segmented Clamped Windows.

The maximum window surface slope, θ , induced by a deflection of δ is given by

$$\theta = \tan^{-1} \frac{4\delta}{A/n} \quad (2.3-20)$$

Consequently, there is a constraint on the thickness for any chosen maximum allowable θ . The maximum allowable aspect ratio can be seen to vary as $\theta^{1/3}$, for small θ . If, for example, we specify $\theta < 30\mu\text{rad}$ then the aspect ratio for each window must be less than 2.1.

$$\frac{A}{tn} < 2.1 \quad (2.3-21)$$

If θ may run as large as $300\mu\text{rad}$, which may be acceptable at normal incidence, then an aspect ratio of 4.6 is allowed.

$$\frac{A}{tn} < 4.6 \quad (2.3-22)$$

Frames for Segmented Windows

There are many design options for window frames. One simple design is to mount the windows between a gridwork of trusses. If the gridwork has three dimensional extent, the design becomes much more stiff. An approximate governing equation for the deflection, δ , in terms of the parameters shown in Figure 2.3-3 is

$$\delta \approx \frac{1 \times 10^{-7} A^4}{Ha} \quad (\text{cm}) \quad (2.3-23)$$

If we insist that the angular deflection, θ , be less than $300\mu\text{rad}$, the constraint on angular deflection is

$$300\mu\text{rad} > \frac{\delta}{(A/2)} \quad (2.3-24)$$

Figure 2.3-3. Support Frame for an $n \times n$ Segmented Window.

Using Equations (2.3-20) and (2.3-23) with the physical properties of aluminum the support cross section, a , must satisfy

$$a > 33 \text{ cm}^2 \quad (2.3-25)$$

The beams used for support must be about 6cm on a side, which is a tractable support size.

2.3.3 Window Heating

Small amounts of energy absorbed by amplifier windows will cause distortion of the transmitted beams. The window material is assumed to be quartz (coated for minimum reflection and protects from attack by fluorine).

Energy Absorbed per Pulse

One and two photon absorption are described by

$$\frac{dI}{dz} = -\alpha I - \beta I^2 \quad (2.3-26)$$

For quartz, $\alpha \approx 4 \times 10^{-3} \text{ cm}^{-1}$ [2.3-5] and $\beta \approx 4.5 \times 10^{-5} \text{ cm/MW}$ [2.3-6]. Assuming a temporally square light pulse with $I_0 = 7 \text{ MW/cm}^2$ and pulse length 300ns, there are 2.1 J/cm per pulse incidence, and the energy absorbed per unit volume per pulse is

$$Q = 300\text{ns } I_{\text{abs}} = 9.0 \times 10^{-3} \text{ J/cm}^3 = 2.1 \times 10^{-3} \text{ cal/cm}^3$$

Temperature Rise Per Pulse

In steady state, the temperature increase due to each pulse equals the temperature decrease in the interval between pulses. The temperature change is $\frac{Q}{\rho c}$, where ρ is the density and c is the heat capacity.

The solution is

$$T = \frac{-\dot{q}x^2}{2k} + \frac{\dot{q}Lx}{k} + T_0.$$

If we let $\theta(x) = T(x) - T_0$, the temperature difference between the two walls is,

$$\theta(L) = \frac{\dot{q}}{2k} L^2 = \frac{2.1 \times 10^{-2}}{2(3.5 \times 10^{-3})} = 3.0 L^2$$

This is plotted in Figure 2.3-4 as a function of L .

Transient Response

To calculate the temperature at any time t after the start of repetitively pulsed operation, the inhomogeneous heat equation must be solved.

$$a \frac{\partial^2 \theta}{\partial x^2} - \frac{\partial \theta}{\partial t} + \frac{\dot{q}}{pc} = 0$$

where $a = \frac{k}{pc}$ is the thermal diffusivity and $\dot{q}$ is the heat absorbed per $\text{cm}^3\text{-sec}$

(2.3-30)

$\dot{q}$ is a constant in t and x (from 0 to L). The boundary conditions are:

$$\theta(x, 0) = 0$$

$$\theta(0, t) = 0$$

$$(\partial \theta / \partial x)_{L, t} = 0$$

This may be solved by Laplace transforms, but a very complicated expression is obtained. A much simpler solution results from the Green's function method ^{2.3-7}. After some work, this will give:

$$\theta(x, t) = \frac{16bL^2}{\pi K} \sum_{n=0}^{\infty} \frac{1}{(2n+1)^3} \sin \frac{(2n+1)\pi x}{2L} \left(1 - e^{-\left(\frac{\pi}{2L}\right)^2 (2n+1)^2 a t} \right)$$

(2.3-31)

Figure 2.3-4. Temperature Rise in Steady State across a Quartz Window Receiving 2.1 J/cm^2 Irradiance at 10 Hz.

It is easily verified that this solution obeys the heat equation and satisfies the boundary conditions. Furthermore, as $t \rightarrow \infty$, it approaches the steady-state solution found earlier.

The solution is plotted in Figures 2.3-5 and 6 for quartz windows using

$$b = 2.1 \times 10^{-2} \text{ cal/sec cm}^3$$

$$a = 8.62 \times 10^{-3} \text{ cm}^2/\text{sec}$$

and two values of L ($L = 2.5$ and $L = 5.0$ cm)

Notice especially the L^2 dependence of the time constant.

Convection

In the preceding analysis the assumption was made that one face of the window was held to a constant temperature. Actually the face temperature will rise during operation. The convective cooling of the face will stabilize its temperature. Including convective effects, an accurate determination of the temperature distribution in the window would require a significant computational effort and will not be pursued here. The preceding results can be regarded as guides in understanding the gradients in temperature across the window, and probably are somewhat of an underestimate of the gradients transverse to the face of the window.

Optical Effect of Window Heating

Using Equation (2.3-28), we find that the optical path through a 5cm window changes by about three wavelengths ($\lambda = .25\mu\text{m}$) for each one degree increase in temperature. For a single pulse we found the temperature rise was only $.0053^\circ\text{C}$, and the aberration was negligible. However, from Figures 2.3-4,5 and 6, we see that in a repetitively pulsed system, the temperature rise is many degrees. If this temperature is not uniform, the window aberration may be many waves. Thus, if material windows are to be used in a repetitively pulsed system, it is possible that adaptive control of the wavefronts will be required to compensate for aberrations due to nonuniform window heating. For

Figure 2.3-5. Transient Thermal Response of a 2.5 cm Thick Window Receiving 2.1 J/cm² Irradiance at 10 Hz.

Figure 2.3-6. Transient Thermal Response of a 5 cm Thick Window Receiving 2.1 J/cm^2 Irradiance at 10 Hz.

a single pulse test facility the heating of the windows is not a concern to optical quality.

2.3.4 Shock Effects

The amplifier windows will be subject to the overpressure in the laser cavity of approximately 2 atmospheres (28psi) and internally generated pressure changes due to heating of the glass. The latter pressure can be estimated to be $\alpha Y \Delta T$ where ΔT is the temperature rise per pulse (.0053°C), α is the coefficient of thermal expansion ($5.5 \times 10^{-7}/^{\circ}\text{C}$) and Y is Young's modulus ($10.1 \times 10^6 \text{psi}$). The pressure jump due to the rapid, but small, heating from one laser pulse is therefore 0.003psi. The effects of the 28 psi gas overpressure should be more important. The situation will be virtually the same as would be encountered with a large CO_2 laser utilizing material windows except that glass windows are generally stronger than salt windows. For single pulse operation, we do not currently see difficulties arising, though in a repetitively pulsed system there may be deterioration of the windows due to accumulation of fatigue from repeated overpressure shocks.

In a recent discussion^{2.3-8} it has been pointed out that a shock wave may be focused inside the amplifier volume by curved or spherical surfaces and that these converging wavefronts may lead to material damage. In that discussion, the amplifier design included a dome shaped full-size window; (the dome shape gives the window greater strength as compared to a flat window of the same thickness) and this surface provided the spherical focusing. In this study only flat window designs have been considered so that the amplifier should be quite immune to this effect. Nevertheless, this damage mechanism should be considered in more detail as the flat windows will acquire some small curvature due to the static overpressure.

2.3.5 Aerodynamic Windows

The objective in using an aerodynamic rupture or shutter window is to avoid the problems inherent with a large high power material window for the main amplifier. These include thermal and structural problems in producing and maintaining figure while supporting a pressure difference, and problems associated with reliability, segmentation, obscurations, beam attenuation, aberrations, possible damage and cost. Though none of these problems is insurmountable, building a large material window, perhaps segmented, for the power amplifier represents a major effort. In this section, the possibility of using an aerodynamic window as an alternative to the material windows will be explored.

Full Aperture Aerodynamic Windows

Calculations for a supersonic shock-expansion type of aerodynamic window show that the power requirement is minimized for an array of Mach 1.5 jets. Higher Mach numbers incur large supersonic diffusion losses and lower Mach numbers incur large subsonic diffusion losses. An individual jet pressure ratio of 2 insures a stable jet without condensation. An array of seven jets across a 100 cm x 100 cm aperture will require 215 kW of pumping power. It will be 50 cm deep and circulate 600 lbm/sec of window gas. Another 20 kW will be required to purify this window gas. The window will introduce a second order curvature effect of 8 fringes at 248 nm. As well as a random unsteady shear layer turbulence shift of nearly 1 fringe at 248 nm.

These results show that the full aperture aerodynamic window is prohibitively costly and introduces unacceptable phase aberrations. As a result, a smaller aperture gas window has been considered.

Reduced Aperture Aerodynamic Windows

Two types of reduced aperture gas windows have been studied; a rotating shutter window and a scaled down version of the full aperture window. For the rotating shutter concept, a three element shutter with a 10 cm x 10 cm aperture was designed to operate with 10 openings per second; each of 400 microsecond duration.

A pumping power of 1 kW would be required to balance the leakage through a window of this kind. The power requirements for both the rotating shutter and reduced full aperture window appear acceptable but the optical beams would have to be directed through these smaller windows using focusing optics. Since the window would be located near the focal region, there may be a problem with possible breakdown in the low pressure region downstream from the gas window. There is a need for experimental breakdown measurements at wavelengths of $0.25\text{ }\mu\text{m}$ in the region of 1 mtorr to 1 torr before such a concept can be considered entirely feasible.

Further, there are serious difficulties in the optical design of mirrors used to transport the optical beam through these smaller windows. To illustrate these optical problems, a possible design is shown in Figure 2.3-7.

Entrance to the amplifier chamber can be through a low flux material window which is not as large as the amplifier in cross section. The beams then enter the amplifier expanding. If the beams were to enter collimated, the material window would need to be a full aperture element. This element differs from the window in a material window design in that it is to operate at low flux loading. However, if a full aperture material window is required, it is improbable that the nonmaterial window design will offer any advantages over the material window design. The beam will therefore be considered to enter the amplifier expanding. A single mirror or an array of small mirrors can be used to direct the beams into the amplifier. Any single element used to direct the beams through focus and into the amplifier will have to accommodate the spread in the angularly encoded array that will be spatially separated near an image plane. In order to optimally extract energy from the amplifier, the pupil plane will be within the amplifier. If the pupil is to be centered along the optical path, it will be located at the mirror designated M2 in Figure 2.3-7.

AERO, RUPTURE OR
ROTATING WINDOW DESIGN

Figure 2.3-7. Optical Design of an Amplifier Utilizing a Non Material Window. The beams are separated at image planes.

In order to reduce the passage through the dead zones between both the low flux window and the pumped region of the amplifier and between the pumped region of the amplifier and the following nonmaterial window, it is important to keep these distances short. A practical limitation is that as mirrors M2 and M3 get very close to each other they must work well off-axis to avoid vignetting the entering and exit beams. In addition the design must be additionally set off-axis to allow space for mounting and mechanisms associated with the material and nonmaterial windows. As the distance between the low flux material window and M2 and between M3 and the nonmaterial window are reduced, the f numbers of the mirrors become lower. The limitations imposed here are twofold; fast off-axis elements are difficult and costly to fabricate, and fast off-axis elements working over a finite field will introduce aberrations. There is the necessity to work over a finite angular field because the beams are angularly encoded arrays and the use of a nonmaterial window does not lend itself to an axis-asymmetric (constant field angle) design. The elements would be likely to require aspheric correction to optimize their performance off-axis. Consequently, their complexity and the difficulty in fabrication will exceed that of comparably sized optical telescopes for the ultraviolet.

A simple computation can show how much the optical elements must be oversized if they are not at a pupil. If the optics are taken as $f/3$ and the aperture of the amplifier is 1 meter then the mirrors are spaced by 7 meters. With the beams in a conical array with a half cone angle of $.5^\circ$ the beams will wander $\pm 6 \sin (.5^\circ)$ or $\pm 5.2\text{cm}$. This scales linearly with cone angle, f-number and beam diameter.

To bring the beam through the nonmaterial window mirror M3, requires bringing the beams to focus. Either a line or point focus will reduce the area of the beams so that they can be directed through a nonmaterial window of small aperture. A line focus has lower flux densities, and is less likely to induce gas breakdown. However, to generate a line focus will require two large cylindrical mirrors, M2

and M3, which in general would be more difficult to fabricate and test than large aspheres. In Figure 2.3-8, there is a map of the aberrations induced by passage through a relay composed of two cylindrical elements. Ray-traces of an equivalent refracting set-up, a pair of reflection parabolic cylinders, and pairs of off-axis Mangin cylinders (Mangin mirrors are mirrors with a thin refracting lens in contact) showed rms aberrations of one or more waves ($.25\mu\text{m}$) in all cases for the most aberrated beam in the array. The Mangin cylinders gave the best performance, but are partially refractive and may be impractical. The most important result is that the aberration, which was primarily astigmatism, differed for each beam in a conical array, and the axis on which the tangential and sagittal rays are defined is rotated for each different beam. Consequently, it is impossible to simultaneously correct for the astigmatism of all the beams using a single optical element.

The optical design for an amplifier with a nonmaterial window will require three or more fast off-axis aspheric elements. The issues of costs, reliability, and aberrations with this design weigh against it. The advantage is clearly avoiding a high flux large material window which may be prone to damage and aberration, but which can be accommodated into more advantageous designs.

Alignment Constraints

The use of a nonmaterial window creates difficulties for end-to-end alignment of the system. Alignment of a system employing an aerodynamic window will be discussed first. Consider using an auxiliary alignment beam for end-to-end alignment. If the alignment beam is directed through the amplifier while the aerodynamic window is not operating then the alignment may not be true when the gas is flowing through the window and the system is brought to its operating pressure. The system can either be aligned with the aerodynamic window operating, with the aerodynamic window replaced by a material window used during alignment only, or with the aerodynamic window not operating. In a

[illegible]

Figure 2.3-8. An Optical Path Difference (opd) Map for One Beam of a Conical Beam Array After Passing through a Pair of f/4 1 Meter Parabolic Cylinders. The azimuthal angle, ϕ , for this beam was 45 degrees. The opd map will vary with ϕ . The half cone angle of the array was 0.5 degrees and the map is given in wavelengths at 6328 Å.

high repetition rate system alignment with the aero window operating is feasible. In a single pulse test facility where rapid alignment is not required the alignment will have to be accomplished with either a replacement material window used only during alignment or with the delay lines at high pressure. Alignment would have to be insensitive to these changes in the optical system. In one case the replacement window would have to be such that it did not deviate or defocus the alignment beam. If no replacement window is used then the optical elements after the main amplifier will have to be mounted so as to be insensitive in alignment to changes in the beam line pressure, and the alignment and focus must not be affected by the index of the gas. With a shutter or rupture window there will be a fixed obscuration in the optical path to hold off pressure until moments before the laser is fired.

End-to-end alignment, or even alignment of the amplifier as a subunit, will be made more difficult by a shutter or rupture window. Alignment could be accomplished by alignment up to the window from the front end and from the window to the target, each accomplished using some specially designed retractable alignment references located at the window. However, it appears less complex and more accurate to devise a way to replace the rupture or shutter window with a material window during the alignment process. Again the material window used during alignment must be carefully designed and mounted so as to have no effect in the alignment or focus, and yet be easily inserted and removed. Because the window is in convergent light, if the window is simply flat, it would introduce spherical aberration. It is likely it would have to be a shell.

Regardless of the kind of nonmaterial window used, it is apparent that for a single pulse test facility end-to-end alignment of the actual optical system is not practical, unless alignment can be accomplished during brief moments when a shutter is open or if the aerowindow can be run continuously. For a functional power plant this constraint is not important, but for a single pulse test facility it weighs against the use of nonmaterial windows.

2.4 Prepulse Suppression

Prepulse damage will occur if light is misdirected through an optical path out of correct temporal sequence. Spatial filters will be used to maintain high beam quality and only light that passes through these filters will reach the target. Light from one pulse can go into another beam line as the result of scatter, edge diffraction, deflection in turbulent beam paths and perhaps other mechanisms. Because each beam line has a different delay, the scattered light will arrive at the target early or late. Isotropic spontaneous emission may also act as a source of light into beam paths which will result in prepulse damage. The spontaneous emission will be amplified when passing through successive amplifier stages.

Cross talk will be small in intensity with respect to the total energy transmitted to the target. The system will amplify the main pulse from stage to stage. If cross talk is simultaneously present, it may not be subject to identical amplification. In a system employing angular multiplexing, the amplifiers are not completely filled by each beam. Consequently, the cross talk may pass through regions of unsaturated gain and be preferentially amplified with respect to the main pulses. Saturable absorbers may be used in the regions near foci, where the angularly encoded beams are separated. This offers a means for preferentially attenuating the weak cross talk signals. Ozone, for example, has been discussed as a possible saturable absorber at 250 nm.^{2.4-1} The degree to which signals contributing to cross talk are preferentially amplified or attenuated will be an important parameter in determining the amount of prepulse energy that will arrive at the target.

2.4.1 Surface Scatter

The relative importance of scatter as opposed to diffraction increases rapidly with decreasing wavelength. The total integrated scatter (TIS) increases roughly as one over the square of the wavelength.^{2.4-2}

$$\text{TIS} = (4\pi\delta/\lambda)^2 \quad (2.4-1)$$

where δ is the rms surface roughness for a Gaussian surface.

As the number of beams from a single module is increased, with the field angle into which the array is sent held constant, the angle through which light must scatter to cause cross talk is reduced and the number of scattering and receiving beam lines is increased. Scatter-induced cross-talk will place a limit on the number of beam lines that can be used in a single module. For candidate optical designs and encoder designs, diffraction is not limiting. This will be explained further below.

Consider an angularly encoded array where the beams are directed on the surface of a cone of half angle τ . Adjacent beams are separated by an angle θ given by

$$\theta = 2\pi\tau/N \quad (2.4-2)$$

where N is the number of beams. The expression for scatter as a function of angle, θ , can be written as

$$\sigma = \begin{cases} (\delta/30)^2 \left(\frac{2500\text{\AA}}{\lambda}\right)^2 \frac{1}{20\theta} & \text{for } \theta < .01 \text{ radians} \\ (\delta/30)^2 \left(\frac{2500\text{\AA}}{\lambda}\right)^2 \frac{1}{2000} \theta^2 & \text{for } \theta > .01 \text{ radians} \end{cases} \quad (2.4-3)$$

agreeing with the data of Smart.^{2.4-3} σ is the fraction scattered per steradian.

For small angles appropriate to the present concern, the intensity of scattered light varies roughly as $(1/\theta)$. The magnitude of the scatter was chosen very conservatively for the following, extrapolating from the data of Bennett et al,^{2.4-4} for surfaces of 30 Å rms roughness corresponding to about 2 percent total integrated surface scatter.

Scatter into a beam path leading to prepulse arrival requires that light be sent into a spatial filter on a beam path leading to target. If the spatial filters are tight, scattering into the filter is a necessary and sufficient condition that the light will reach target. This configuration is shown in Figure 2.4-1 in which light is scattered

Figure 2.4-1. Mirror and Spatial Filter For Control of Beam Quality

during reflection from the surface of a mirror. The specular reflection enters hole A, and some of the scattered light will go into an adjacent hole, B, and progress with an improper temporal delay through the optical train to the target. To pass into hole B, the light must be scattered into the small angular subtense, Ω , occupied by the hole in the filter which is located at the image plane of the preceding mirror. Consider the hole to have a radius of twice the Gaussian beam radius at the waist, w , and be a distance of ℓ from the focus. The angle subtended by the hole in the filter is

$$\Omega = 4\pi w^2 / 4\pi \ell^2 \quad (2.4-4)$$

Exactly the same considerations are applicable to determining energy directed to the target out of temporal sequence by amplified spontaneous emission, by diffraction from hard apertures, and from nonuniformities in the beam path. The diffraction-limited spot radius, w , is approximately λf_n where f_n is the f-number of the focusing system. Conservatively, if the hole is assumed to have twice this radius, then

$$\Omega = \frac{\pi w^2}{4\pi \ell^2} = \frac{\pi (2\lambda f_n)^2}{4\pi \ell^2} = \frac{\lambda^2}{d^2} \quad (2.4-5)$$

and the angular subtense accepting scattered light into a channel leading to cross talk depends only on the beam diameter and the wavelength. For $\lambda = 0.25 \mu\text{m}$ and $d = 0.10 \text{ m}$

$$\Omega = 0.625 \times 10^{-11} \text{ steradians} \quad (2.4-6)$$

The fraction of the light incident upon a mirror which is scattered through an angle θ into an angular subtense Ω giving cross talk between two adjacent channels is $\sigma\Omega$:

$$\sigma\Omega = 5 \times 10^{-14} (N/d^2\tau) (\delta/30)^2 \quad (2.4-7)$$

where N is the number of beams, d is the aperture of the mirror, τ is the half cone angle of the beam array and δ is the rms surface roughness of the mirror in $\text{\AA}$. If $\delta = 15 \text{ \AA}$, $N = 50$ beams, $d = 0.10 \text{ m}$ and $\tau = 0.009 \text{ rad } (0.5^\circ)$ then

$$\sigma\Omega = 7 \times 10^{-9} \quad (2.4-8)$$

Each scattering site results in light being misdirected down N separate channels, about roughly half will not result in prepulse arrival, but rather postpulse arrival. For nonadjacent beams, the scattering diminishes with angular separation as $1/\theta$. The total contribution to prepulse energy will be linear in the number of scattering surfaces in the whole optical train. Taking the number of optical surfaces, n_s , to be 50 and the number of beams to be 50, the prepulse energy will be smaller than the total energy by a factor of

$$5 \times 10^{-14} \frac{n_s (\lambda n N) N}{d_o^2 \tau} \left(\frac{\delta}{30} \right) = 1.4 \times 10^{-6} \quad (2.4-9)$$

For a megajoule system, the prepulse energy would be about 1 joule. The prepulse could be decreased by increasing the cone angle, decreasing the surface scatter or reducing the number of beams. Simple analysis shows that the prepulse energy will be greatly reduced if there are saturable absorbers which selectively attenuate weak signals. The situation is worsened if weak signals are preferentially amplified by unfilled and unsaturated portions of the amplifier medium. It appears that attenuation of prepulse arrival using saturable absorbers will be required.

2.4.2 Diffraction

The amount of diffractive cross talk can be estimated assuming that there is a single limiting aperture in the system and calculating the cross talk that results. The situation depicted in Figure 2.4-2 will be analyzed.

The principal ray is diffracted by the aperture in the x-z plane. For simplicity, the aperture is taken to be circular. Another choice of aperture would not alter the qualitative aspects of this problem.

Figure 2.4-2. Diffraction at a Circular Aperture.

The principal and scattered rays are both at an angle of τ , the half cone angle, off the optical axis. This is necessary if the diffracted beam is to be a source of cross talk, because it must be directed along one of the beams in the conical array to arrive at the target. The diffracted ray is rotated with respect to the unscattered ray by an angle ϕ in the x-z plane. The angle between the principal ray and the diffracted ray is $\phi\tau$ in the small angle approximation. The differential expression for the diffracted power into a solid angle $d\Omega$ at an angle τ off the optical axis and through an azimuthal angle of τ is^{2.4-5}

$$dP = P_0 \frac{(ka)^2}{4\pi} (\cos^2\phi + \cos^2\tau + \sin^2\phi) \left| \frac{2J_1(ka\xi)}{(ka\xi)} \right|^2 d\Omega \quad (2.4-10)$$

where P_0 is a constant
 dP is the incremental power falling into a solid angle
 $d\Omega$ at angular coordinates (τ, ϕ)
 $k = 2\pi/\lambda$ is the wave number
 J_1 is the first order Bessel function
 a is the radius of the hard aperture and
 $\xi = (2\sin^2\tau - 2\sin\tau\sin\alpha\cos\phi)^{1/2}$

The half-cone angle, τ , will be taken to be small, $|\tau| \ll 1$, then $\sin\tau \approx \tau$. If there are N beams, then adjacent beam lines will diffract into each other if $\theta = 2\pi/N$. Using the small angle approximations for sine and cosine gives

$$\xi \approx \alpha\phi \quad (2.4-11)$$

The ratio of the intensity of the diffracted light to that of the un-deviated light is

$$\frac{(dP/d\Omega)_\phi}{(dP/d\Omega)_{\phi=0}} = \left(2 + \frac{\phi^2}{2} - \frac{\tau^2}{2}\right) \left(\frac{1}{2}\right) |2J_1(x)/x|^2 \quad (2.4-12)$$

where $x = ka\xi = ka\alpha\phi$. For the KrF scaling module, $k = 2.5 \times 10^5 \text{ cm}^{-1}$, a is between 1 and 100 cm in aperture, ϕ is about 0.1 radian and α is about 0.01 radian and so x is of the order of 2500. Therefore, the

asymptotic expression for large argument can be used for J_1 :

$$|2J_1(x)/x|^2 \approx \left| \frac{2}{x} \sqrt{\frac{2}{\pi x}} \cos \left(x + \frac{3\pi}{4} \right) \right|^2 \approx \frac{2}{\pi x^3} \quad (2.4-13)$$

The second approximation is made by noting the average of cosine squared is 1/2. Consequently, with the half-cone angle written in degrees as β ($\beta = 180\tau/2\pi$):

$$\frac{(dP/d\Omega)_{\phi}}{(dP/d\Omega)_{\phi=0}} = \left(1 + \frac{\phi^2}{4} - \frac{\tau^2}{4} \right) \frac{2}{\pi} \frac{1}{x^3} \approx \frac{2.1 \times 10^{-16} N^3}{a^3 \beta^3} \quad (2.4-14)$$

where again N is the number of beam lines and a is the aperture in cm.

The diffracted light falls off in intensity as the cube of the diffraction angle; consequently, only diffraction between adjacent beam lines requires consideration. Moreover, the intensity of the diffracted light is characteristically so much smaller than that of the main beam that it does not approach a part in 10^{10} for a 5 cm limiting aperture until N nears 400. Diffraction is not an important source of cross talk.

2.4.3 Spontaneous Emission

A similar starting point to that used for scatter and diffraction can be used in determining the contributions of spontaneous emission to cross talk. Again, only that portion of the emission directed into the small angular subtense occupied by the spatial filters will pass to the target. From the calculations in Section 2.4.1, we see this angle is less than 10^{-11} steradians. The noise energy, dE , due to spontaneous emission from a volume of gas dV in a spectral range, $d\nu$, and directed into a solid angle $d\Omega$ is^{2.4-6}

$$dE = \frac{h\nu N_2 g(\nu) d\Omega d\nu dV}{4\pi\tau_{\text{spont}}} \quad (2.4-15)$$

where h is Planks constant

ν is the frequency of the radiation

N_2 is the population of the upper laser level

$g(\nu)$ is the line shape

τ_{spont} is the spontaneous emission lifetime

The expression for the gain, γ , is^{2.4-6}

$$\gamma = \frac{(N_2 - \frac{g_2}{g_1} N_1) \lambda^2 g(\nu)}{8\pi n^2 \tau_{\text{spont}}} \quad (2.4-16)$$

where N_1 is the population of the lower laser level

λ is the wavelength

g is the degeneracy of the i -th laser level

n is the index

Assuming $N_2 \gg N_1 \frac{g_2}{g_1}$ and $n = 1$

$$dE = \frac{2hc\gamma n^2}{\lambda^3} d\Omega d\nu dV \quad (2.4-17)$$

Taking values appropriate to the front end of the KrF system

$$\lambda = 0.25 \times 10^{-4} \text{ cm}$$

$$d\nu = \lambda^{-2} d\lambda \text{ where } d\lambda = 0.0002 \times 10^{-4} \text{ cm}$$

$$dV = 7 \text{ cm} \times 7 \text{ cm} \times 100 \text{ cm} = 5 \times 10^3 \text{ cm}^3 \text{ (typical gain volume in the encoder)}$$

$$d\Omega = 10^{-11} \text{ steradians (defined by spatial filter apertures)}$$

$$\gamma = 0.05/\text{cm}$$

and knowing $h = 6.62 \times 10^{-27} \text{ erg-sec}$ gives

$$dE = 2 \times 10^{-9} \text{ erg/sec} = 2 \times 10^{-16} \text{ J/sec} \quad (2.4-18)$$

This energy, emitted from the front end, is amplified on the way to the target. Generously, let us assume a power gain of 10^5 through the system for the full spectral bandwidth of the spontaneous emission. (MJ system with a 10 J encoder). Then the energy reaching the target due to amplified spontaneous emission in a pulse time τ due to the $N/2$ channels giving early arrival of energy is $10^5 N\tau dE/2$. For $\tau \approx 0.5 \text{ sec}$ and $N = 50$, the prepulse energy is

$$\text{Prepulse} = 5 \times 10^{-15} \text{ J} \quad (2.4-19)$$

Again, preferential amplification by unfilled regions in the amplifiers or preferential absorption of the small prepulse by saturable absorbers would be effects that could act to respectively increase or decrease this already very small amount of prepulse energy. Clearly, spontaneous emission from the front end and then amplified in the optical train cannot be a serious cross talk problem.

2.4.4 Nonuniformities in the Amplifier Medium

Beam steering due to turbulence can be analyzed using the formula shown in Section 2.2 where beam quality in a turbulent beam path is discussed. The rms steering angle σ_θ is roughly the rms phase fluctuation across an aperture divided by the aperture dimension.

$$\sigma_\theta(D) = \sigma_\phi(D)/kD$$

Using $\sigma_\phi = 1.7 C_n k \sqrt{R} D^{5/6}$, we obtain σ_θ as

$$\sigma_\theta(D) = 1.7 C_n \sqrt{R} D^{-1/6} \quad (2.4-20)$$

where C_n is the turbulent structure constant and is about 8×10^{-9} for weak turbulence

R = the transverse dimension of the medium ($R \approx 1$ m for the amplifiers)

D = the aperture ($D \approx 1$ m for the amplifier)

For the final amplifier

$$\sigma_\theta(D) = 10^{-8} \text{ radian} \quad (2.4-21)$$

Because this is far less than the spacing between beams (typically of the order of a milliradian) we expect that cross talk will be negligible due to this source.

The presence of strong turbulence could increase σ_θ by two orders of magnitude. Limiting difficulties might appear only if there were thermal variations across the amplifier, which in conjunction with small scale turbulence resulted in beam spreading. Some analysis of this problem has been carried out by Haas et.al.^{2.4-7} where assumptions were made concerning the scale of the turbulence and its structure constant C_n .

Using these assumptions, an RMS steering angle is determined and a distribution of "refracted" power out of the main beam can be written down in terms of a Gaussian function. The fractional energy refracted into adjacent beam lines is then calculated. The results of these computations show that a nonuniform amplifier medium could become the most serious cross talk problem as the angular separation between beam lines becomes very small. However, these results are very sensitive to the optical multiplexing geometry and the nature of the turbulence in the amplifier medium. Consequently, this source of cross talk must be investigated further in the second phase experiments of this program.

2.5 Optical System Configuration and Alignment

A functional block of the optical system is shown in Figure 2.5-1 for the purposes of clearly defining the operations involved in beam transfer and stacking. Between stages of amplification are relay optics which are used to image one stage (amplifier pupil) onto a subsequent stage. The relays will include spatial filters and, if necessary, beam preseparators to control angular field and beam size.

Double pass amplifiers are used throughout the system as a reasonable compromise between energy extraction efficiency, optical quality and the desire to use spherical optics without obscuration. The angular encoder produces nested circular arrays of beams, the nesting being used to shorten beam separation lengths and reduce the volume of the evacuated beam ducts.

The basic elements or building blocks of the optical train are shown in Figure 2.5-2. Note that the relay mirrors (mirrors used to double pass each amplifier) work at unity magnification, that is, they relay the image (focal spots or spatial filler apertures) symmetrically about the center of curvature. This concept can be used in two ways: (1) by creating semi-circular arrays of beams with encoder and relaying these beams across the centerline so that the amplifier stages are actually nested, or (2) using full

Figure 2.5-1. Baseline Block Diagram.

Figure 2.5-2. Basic Elements of the Module Optical System.

circular arrays and folding the optic axis in a zig-zag pattern. Examples of these approaches are given in Figures 2.5-3 and 2.5-4. Figure 2.5-3 shows an example of a nested optical system. In this configuration, the encoder (semicircular array) output is relayed from the half-plane above the axis to the half-plane below the axis by relay mirror number 1. The subsequent relays work in similar fashion. This avoids obscuration problems and the relay mirrors work at a given field angle without tilt. Field lenses (not shown) are required near the image planes to relay pupils from one stage to the next. The advantage of this approach is that all critical optical elements are co-located on a common optical bench or space frame to minimize relative motion and misalignment.

An example of a folded optical configuration using full circular arrays is shown in Figure 2.5-4. In this configuration, the encoder output is a full circular (or nested circular) array of beams which is brought to a ring image and relayed as before through the amplifier train. The principal difference from the nested approach described above is that the relay mirrors are used with a small tilt which leads to a slightly aberrated final beam (if spherical relay mirrors are used). Such aberrations are correctable within the system and the resultant optical quality is still nearly diffraction limited. An advantage of this design is that it has shorter separation lengths and consequently should be easier to align. The beam path conditioning requirements will also be eased due to this length reduction. Another advantage is that the center of curvature of each relay mirror is located in a convenient space between image planes and co-located on a central optical bench. This allows for center of curvature alignment sensors which can control the alignment of the relay mirrors to a common stable reference. Further details on alignment will be discussed separately below.

2.5.1 Optical Configuration Design Logic

Before discussing alignment it is useful to review the design logic behind the optical configurations shown above. This can be best understood in the context of a particular design followed by a review of the scaling

Note Use of "Deformed Rhomboids"
to Increase the Beam Field Angle

Figure 2.5-3. The Assembled Nested Optical System.

Figure 2.5-4. Folded Optical System Using Full Circular Arrays of Beams.

issues. Let us use the folded configuration of Figure 2.5-4 above.

For the total optical system, encoder pupil to image plane beyond the final amplifier, the following relation holds:

$$\text{Magnification} = \frac{M_{R_1} M_{R_2} M_{R_3}}{M_{E_1} M_{E_2} M_{E_3}} = \frac{\text{Output Beam Angle}}{\text{Input Beam Angle}}$$

where M_{E_1} is magnification of each expander as determined by apertures of pupils and M_{R_1} is magnification artificially introduced by pairs of fold mirrors arranged as a "deformed rhomboid" and located near each intermediate image plane.

The appropriate values for M_{E_1} are determined from gas amplifier design considerations involving optic sizes, power levels and damage thresholds. In this baseline system, the input beam is square with edge $A = 5$ cm. This beam is expanded in three stages to 7, 45, and 200 cm so $M_{E_1} = -1.4$, $M_{E_2} = -6.428$, and $M_{E_3} = -4.444$. The negative signs indicate that the images are inverted.

The values for M_{R_1} are determined so the rhomboid mirrors lie between the planes where the flux equals 2 J/cm^2 (assumed damage threshold) and where the beams are separated into a ring of contiguous circles of diameter $A_i \sqrt{2}$. The distance, $S_{\min}$, from the former plane to the associated image plane is determined from the following equations:

$$d_{\min, i} = \sqrt{E_i / N \epsilon}$$

where E_i is the total energy in the array of $N = 84$ beams and ϵ is the assumed damage threshold flux of 2 J/cm^2 , and

$$S_{\min} = d_{\min} \sqrt{2 (f\text{-no})}$$

where (f-no) is the relative aperture of the propagated beam. This (f-no)

is determined by the optical parameters at the final separation and is constant throughout this subsystem.

The location of the separation plane is found from the following relationship:

$$S_{sep} = \frac{H'}{\left(\frac{N_3 \sqrt{2}}{2\pi(f-no)} \right) - \tan U_{PR}'}$$

where H' is the Gaussian Image Height in the focal plane (computed from EFL $\tan U_{PR}$), N_3 is the number of beams in the outer ring of beams (here equal to 35) and U_{PR}' is the principal ray angle with respect to the beam array

axis immediately following the field lens at the image. The edge dimension of the square beam at separation determined from:

$$d_{sep} = S_{sep} / \sqrt{2} (f-no)$$

The value of the $(f-no)$ comes from consideration of the convergent beam array exiting the final amplifier. In order for the beams to separate, they converge towards a ring field image at a large distance from the amplifier relay mirror. The size of the beam at separation is set at 50 cm diagonal from Target Chamber Window aperture limitations. (Assumption made here is that the target chamber has a material window.) Hence $d_{sep} = 50/\sqrt{2} = 35.35$ cm. The radius of the ring of contiguous circumscribed circles is given by:

$$R_{sep} = \frac{N_3 d_{sep} \sqrt{2}}{2\pi}$$

and is 271.81 cm in this baseline system. The image distance of the final relay mirror is given by:

$$L' = \frac{N_3 d_{sep} \sqrt{2}}{2 \tan U_{PR}^1 (A - d_{sep})}$$

where U'_{pr} is the angle of the principal ray relative to the beam array axis after the final relay. In this case, $U'_{pr} = 0.5^\circ$. A is the beam dimension at the final relay mirror and equals 200 cm. From this equation, $\ell' = 38,768.3$ cm. The (f-no) is now computed as $\ell'/A\sqrt{2}$ or $f/137.066$.

The locations of rhomboid mirrors are set somewhat arbitrarily by choosing appropriate mirror sizes, keeping the mirrors well away from the high flux region near the images, allowing plenty of space for field lenses (or mirrors if needed) as well as the considerations listed above. The offset distances represent the lateral distances in the image plane separating the ring field images without the rhomboids and those images with the rhomboids. No attempt has as yet been made to optimize the designs of the rhomboids. It should be noted that the smallest mirror used in a rhomboid has face apertures of 0.30 x 0.42 cm while the largest has face apertures of 12.00 x 16.97 cm. The "magnification" introduced by each rhomboid is computed from the ratio of image height with the rhomboid (offset plus image height without rhomboid) to the image height without the rhomboid. These ratios are 6.729, 4.215 and 1.763, respectively, in this baseline design.

The rhomboids are defined as "deformed" in this system since the mirrors are not parallel to each other. The outermost mirror is, in each case, tilted differently so as to point the offset principal ray at the center of the following pupil. These pupils are at the subsequent relay mirrors.

The magnitudes of the tilts of the relay mirrors are determined from the geometric requirement that the optics near each focal plane adequately clear those near the adjacent focal planes. All focal planes are constrained in this proposed system configuration to be within a central structural assembly for alignment and stability reasons. It is for this reason that the relay mirrors are chosen to work at unity magnification.

Also by working at unity magnification each relay is completely symmetrical, hence coma-free. In the baseline design, the lateral separations of the optical axes at the successive focal planes, as determined by the outermost rhomboid mirrors are

$$\begin{aligned} 45.5 + 191.9 &= 237.4 \\ 191.9 + 338.3 &= 530.2 \\ 338.3 + 338.3 &= 676.6 \end{aligned}$$

Dividing each of these dimensions by twice the relay mirror radius of curvature gives the tangent of the required tilt angle. The angles range from 5.26° to 1.03° in this case. While these tilts are needed in order to prevent beam and/or optical component interferences, they do degrade the image quality to be achieved from the system. The performance of this preliminary baseline design is the subject of a separate analysis given below.

Design Performance

Following the design procedure and formulae given above, we have designed various systems for 50 kJ final amplifier modules using various numbers of beams (N) and either nested circular or semicircular arrays. The results of these calculations are summarized in Table 2.5-1 for 50 kJ systems. A design variable is the output angle, U'_{PR4} , of the final amplifier (conical-field angle) which strongly affects beam separation length. It also drives the angular fields of preamplifier stages and the system beam quality. As U'_{PR4} increases the separation lengths decrease but the need for rhomboid preseparators becomes more critical.

Results of this scaling exercise are shown in graphical form below. Figure 2.5-5 shows how the overall length of the optical system for a 50 kJ module varies with output field angle for various beam arrays. The circular (3 nested ring) geometry at 1.0 degree half-field angle is preferred due to its short length. Also shown in Figure 2.5-5 is the beam separation distance from the final amplifier module.

Table 2.5-1
Minimum Size Optical System Designs (E = 50 kJ)

Circular Field Arrays									
One Ring			Two Rings			3 Rings			
N	49.00		N	49.00		N	49.00		
N'	49.00		N'	28.00		N'	28.00		
U'PP4 (DECI)	.50	U'PP4 (DECI)	.75	U'PP4 (DECI)	1.00	U'PP4 (DECI)	.50	U'PP4 (DECI)	.75
R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00
S4N1N (CHI)	3229.27	S4N1N (CHI)	3552.67	S4N1N (CHI)	4134.92	S4N1N (CHI)	3570.60	S4N1N (CHI)	3895.66
D4SEP (CHI)	22.59	D4SEP (CHI)	22.59	D4SEP (CHI)	22.59	D4SEP (CHI)	22.59	D4SEP (CHI)	22.59
L'4SEP (CHI)	249.12	L'4SEP (CHI)	249.12	L'4SEP (CHI)	249.12	L'4SEP (CHI)	249.12	L'4SEP (CHI)	249.12
EPL1 (CHI)	209.46	EPL1 (CHI)	190.30	EPL1 (CHI)	142.72	EPL1 (CHI)	145.06	EPL1 (CHI)	95.90
P2 (CHI)	10.44	P2 (CHI)	12.24	P2 (CHI)	9.22	P2 (CHI)	9.40	P2 (CHI)	5.99
P3 (CHI)	25.91	P3 (CHI)	37.21	P3 (CHI)	32.91	P3 (CHI)	12.50	P3 (CHI)	0.70
P4 (CHI)	165.94	P4 (CHI)	110.62	P4 (CHI)	82.96	P4 (CHI)	80.04	P4 (CHI)	55.09
P-NO	360.75	P-NO	245.05	P-NO	880.36	P-NO	179.64	P-NO	119.76
OVERALL LGTH	240.75	OVERALL LGTH	173.03	OVERALL LGTH	120.36	OVERALL LGTH	127.03	OVERALL LGTH	84.60
P3-P4 (CHI)	334.69	P3-P4 (CHI)	356.65	P3-P4 (CHI)	267.33	P3-P4 (CHI)	260.40	P3-P4 (CHI)	173.65

Semicircular Field Arrays									
One Ring			Two Rings			3 Rings			
N	49.00		N	52.00		N	52.00		
N'	49.00		N'	28.00		N'	21.00		
U'PP4 (DECI)	.50	U'PP4 (DECI)	.75	U'PP4 (DECI)	1.00	U'PP4 (DECI)	.50	U'PP4 (DECI)	.75
R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00	R4 (CHI)	100.00
S4N1N (CHI)	3470.99	S4N1N (CHI)	3720.63	S4N1N (CHI)	4446.67	S4N1N (CHI)	3670.51	S4N1N (CHI)	4446.66
D4SEP (CHI)	22.59	D4SEP (CHI)	22.59	D4SEP (CHI)	21.93	D4SEP (CHI)	21.93	D4SEP (CHI)	21.93
L'4SEP (CHI)	490.29	L'4SEP (CHI)	490.29	L'4SEP (CHI)	276.37	L'4SEP (CHI)	207.20	L'4SEP (CHI)	207.20
EPL1 (CHI)	370.92	EPL1 (CHI)	300.60	EPL1 (CHI)	316.64	EPL1 (CHI)	237.52	EPL1 (CHI)	190.34
P2 (CHI)	36.20	P2 (CHI)	24.59	P2 (CHI)	15.92	P2 (CHI)	15.21	P2 (CHI)	10.14
P3 (CHI)	51.63	P3 (CHI)	34.42	P3 (CHI)	18.39	P3 (CHI)	14.20	P3 (CHI)	10.20
P4 (CHI)	331.00	P4 (CHI)	221.24	P4 (CHI)	121.60	P4 (CHI)	706.90	P4 (CHI)	91.26
P-NO	737.90	P-NO	491.65	P-NO	407.63	P-NO	304.22	P-NO	202.01
OVERALL LGTH	521.49	OVERALL LGTH	367.65	OVERALL LGTH	298.02	OVERALL LGTH	215.12	OVERALL LGTH	143.41
P3-P4 (CHI)	1069.90	P3-P4 (CHI)	712.90	P3-P4 (CHI)	590.16	P3-P4 (CHI)	441.12	P3-P4 (CHI)	294.07

Figure 2.5-5. Optical System (Module) Length and Beam Separation Distance as a Function of Output Angle for Various Compression Ratios (N). Note that N' is the number of beams in the outermost ring of the array.

The number of preseparators needed to control field angle throughout the optical train is shown in Figure 2.5-6. The field angle at various stages is plotted for a given output angle as a function of the number of "rhomboid" separators. Note that the beam quality limitations on field angle are less severe in the earlier stages as the aberrations are aperture size dependent. For example, a 4 degree field half-angle may be acceptable at the first preamplifier (relay mirror). A 0.5° output angle allows us to use only 2 rhomboids to meet this requirement. Clearly more rhomboids are needed as the output field angle grows beyond 0.5° .

Note that in all these calculations we assume compression ratios $N \geq 49$. This parameter strongly impacts the scale of the system (as shown in the design equations earlier) and we can expect the system to change accordingly for different values of N .

An important design issue is, of course, final beam quality. Since we are using spherical relay mirrors throughout the design the principal aberration which results is astigmatism. Figure 2.5-7 illustrates the problem. The aberration results in a "blur" or growth of the final best image which scales as the square of the field angle. Astigmatism may be controlled, if necessary, with the aid of the field lenses between amplifier stages. The need for such correction depends on ultimate beam quality requirements which are set by pellet design issues and thus not well defined at this time. We have, however, performed a complete ray trace design using $N = 84$ in 3 nested circular arrays (35, 28 and 21 beams) with dimensions as shown in Figure 2.5-8. The wavefront exiting the final amplifier module showed (worst case) approximately 3 waves of astigmatism with no attempt to correct this. (Correction to a small fraction of a wave is achievable.) This wavefront was then propagated using the POP code to the target chamber and focused on a pellet with a final focusing length of 25.0 m assuming a transverse beam size of 35 x 35 cm at the lens. The pellet surface was located 1500 μm from the beam waist to achieve a particular total spot size of 150 μm when illuminated by 84 such beams. Figure 2.5-9 shows the resultant pellet irradiance of a single beam (one of 84) with no aberration (for reference), 3 astigmatism, and for slight

Figure 2.5-6. Requirements for Pre-separators as a Function of Output Field Angle (50 kJ design).

Figure 2.5-7. Aberration Effects for Spherical Relay Mirrors.

Figure 2.5-8. Folded Optical System Using Full Circular Arrays of Beams.

Figure 2.5-9a Pellet Irradiance by a Single Beam Without Aberrations.

Figure 2.5-9b. Pellet Irradiance by a Single Beam with 3 waves of Astigmatism.

Figure 2.5-9c. Pellet Irradiance by a Single Beam with 1.3 waves of Astigmatism.

correction (1.3 astigmatism). The correction applied here was a slight change in curvature of the final relay mirror such that it has a mild toric departure, that is, it has two different spherical curvatures in the sagittal and tangential planes. Better correction, if needed, could easily be achieved. The contour lines in these plots are drawn for 10% increments in intensity.

As noted earlier, we also wish to evaluate the resultant irradiance of the pellet by the full 84 beam array assuming the array is brought to focus as a single source. This has been analyzed with the Perkin-Elmer POP code and results are shown in Figure 2.5-10 for the cases corresponding to the single beam irradiances of Figure 2.5-9. A single quadrant of the intensity contours is shown assuming incoherent addition of the 84 beams. The resultant patterns are not significantly different in size. Irradiance uniformity is slightly different but no attempt has been made to optimize or control this since the requirements for uniformity are not as yet established (or available). It should be noted that these results do not include the effects of subapertures on the beam as we wanted to investigate the impact of only the optical train (relay mirrors rhomboids and field lenses) on the optical quality. The conclusion to be made is that the designs shown here yield reasonable beam quality without effort to correct for residual aberrations.

2.5.2 Optical System Alignment

In this section, we shall address those issues relevant to optical system alignment as it takes place prior to a target shot. This discussion shall assume that initial assembly alignment has been accomplished using standard procedures which do not represent any new technology requirements. We shall omit treatment of alignment during repetitive pulse operation as it requires consideration of issues involving target chamber and pellet design which are beyond the scope of this study.

Alignment issues have been divided into two parts: (1) interstage alignment, and (2) system alignment. Interstage alignment establishes placement of pupils in relation to one another, that is, the critical

Figure 2.5-10(a). One Quadrant of the Pellet Irradiance due to 84 Beams Illumination.
Each of the 84 Beams are Fully Corrected (Unaberrated)

KRFHB NB6246-018 ZERNIKE APPLIED 3.199999E 00
AT ZPEL= 1500 MICRONS..CMESH FOR LAST .4CM
POP 53 03/23/80 11:10:40 INTENSITY 1.100881E 17

Figure 2.5-10(b). One Quadrant of the Pellet Irradiance due to 84 Beam Illumination.
Each of the 84 Beams is Aberrated with 3 waves of Astigmatism.

Figure 2.5-10(c). One Quadrant of the Pellet Irradiance due to 84 Beam Illumination.
Each of the 84 Beams is Aberrated with 1.3 Waves of Astigmatism.

apertures such as amplifiers and relay optics in the optical train. System alignment assures the location and direction of the beams as they transverse the optical train with respect to these pupils and ultimately the fusion pellet.

Interstage alignment is simplified by the axisymmetric geometry chosen for encoding. The need for preseparator rhomboids to control field angle in the amplifiers, however, complicates the alignment requirement since we must assure that each of the N beams are properly directed. Let us consider the alignment of the encoder stage to the first preamplifier through a preseparator. Since we are testing an axisymmetric design we shall only refer to a single representative beam to illustrate the concepts. As we noted earlier, the critical optical assemblies such as beam separation optics are co-located on a stable optical bench or space frame. The encoder optics are likewise co-located with the first preseparator due to relatively small size of the assemblies. Figures 2.5-11 shows the basic alignment steps which can be automated for routine control. Figure 2.5-11 (a) shows alignment of the encoder to the preseparator. A pinhole array is used to properly define the image location of each beam. A reference beam can be used on-axis to allow tilt sensing and control to an on-axis pinhole. A wedge can be inserted at the pupil plane of the encoder to simulate any one of the N beams exiting the encoder. Proper alignment to the reference pinhole can be sensed in several locations including the pupil of the next preamplifier. Final pointing control of each beam can be done by retracting the pinhole array and using the image relaying property of the optical design to position the image of a point source at the encoder pupil on the center of the following preamplifier. Alternatively one can place a point source at the preamplifier pupil and observe its image back at the encoder, using one of the rhomboid mirrors to control position.

The next step in alignment is the subsequent transfer of the N multiplexed beams to the next amplifier stage. This is shown in Figure 2.5-12. Again we can use an on-axis reference beam as above to define the optical

Figure 2.5-11. Alignment of the Encoder to the First Preamplifier Through a Pre-separator Showing (a) Angular Alignment to the Pre-separator Assembly, (b) Angular Field Alignment, and (c) Final Pointing Control.

Figure 2.5 - 12. Beam Transfer and Alignment to the Next Stage for a Folded Optical System.

axis from the encoder through the preamp to the following preseparator assembly (PSA). A pinhole array can be used in PSA-II to locate the reference axis and the N multiplex images. As before, we can use one of the rhomboid mirrors for alignment control of each beam to the next amplifier pupil. The center of curvature of RM_1 is a potential location for a mirror alignment sensor to control the angular orientation of RM_1 . The distance from the stable optical bench to the relay mirrors of the amplifiers grows in each stage and remote sensing and control of the mirror alignment from a stable location out of the beam path is desirable. In this case, the center of curvature represents an auxiliary reference to which the relay mirrors can be aligned. This procedure is repeated in subsequent stages through the final amplifier. After final amplification the beams are not brought to a real focus until they reach the pellet in the target chamber. The on-axis reference beam can nonetheless be used to locate the center of the beam separation plane. As noted earlier, one can place an alignment telescope back at the encoder pupil with an optical wedge to observe alignment sources placed downstream at various reference locations such as amplifier pupils. After final amplification, the beams are separated and we must assure proper location of the mirrors at the separation plane. This can easily be done by placing an alignment source (an LED, for example) at each of the mirrors in the separation plane and observing its image back at the front end. We do not anticipate that this alignment will, in fact, be needed except during initial assembly or following maintenance of the optics. The same procedure can be used right up to the target chamber or to the pellet itself. Alignment to target is not addressed here as it falls beyond the scope of the present study. It is, however, intuitively clear that the axisymmetric optical configuration employed in this design affords alignment advantages not easily achieved in other geometric forms. It appears feasible to use a single alignment laser source and sensor to align the entire optical system from the encoder to the pellet.

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III. KINETICS AND AMPLIFIER SCALING

The rare gas halide (RgH) lasers which operate in the near ultra-violet region are the obvious candidate for any visible or UV source because of their very high demonstrated efficiency.^{3.1-1,2} At present they are the only laser systems which might satisfy the ultimate requirement of at least one percent overall efficiency in the compressed pulse.^{3.1-3} Furthermore, these systems can be efficiently excited by direct electron impact using a broad area electron beam and are therefore scalable, in principle, to large volumes. Both KrF and XeCl are considered to be the leading candidates because of their demonstrated intrinsic efficiency of 10 and 5 percent, respectively.

3.1 KrF and XeCl Lasers

The recent rapid development of the rare gas halide lasers has concentrated most heavily on KrF, which has led to fairly good understanding of the dominant excitation and quenching processes in the e-beam excited system.^{3.1-1} The efficiency of the e-beam excited XeCl laser has been improved recently,^{3.1-2} although our kinetic understanding of this system is not, as detailed as for KrF. A comparison of the best published experimental results is shown in Table 3.1-1. The longer wave length of XeCl, 308 nm versus 249 nm, may have significant impact on the engineering limits imposed by optical surface damage limits.

TABLE 3.1-1 Comparison of published experimental results on KrF and XeCl.

Molecule	KrF ^a	XeCl ^b
Diluent	Ar	Ne
p (amagats)	1.7	4
J _{eb} (amps/cm ²)	11.5*	7
g ₀ (%/cm)	3.7	2.6
α ₀ (%/cm)	.35	.26
E (j/l)	12	3
Efficiency (%)	9	5

^a Reference 3.1-1

^b Reference 3.1 2

The e-beam pumped KrF laser uses an Ar diluent to provide the necessary stopping power for the primary electrons. The best performance to date in XeCl has been obtained with a Ne diluent. In order to provide the same stopping power, i.e., input power density, it is necessary to operate the Ne diluent at roughly twice the pressure for the argon diluent. Both laser systems show intrinsic transient absorption during the excitation pulse. In fact, the use of Ne as a diluent in the XeCl laser is required to minimize the absorption attributed to the dimer ions Ar₂⁺ and Xe₂⁺. At low current densities, of the order of 10 amps/cm², the ratio of small signal gain to intrinsic absorption is approximately 10 for both laser systems.^{3.1-1,4}

The principal motivation for considering XeCl is its longer wavelength, which may significantly impact the overall optical system size, because of its more favorable damage thresholds. However, during this study, the efficiency of XeCl has not been improved above the 5-6% level; this fact coupled with greater uncertainty concerning the formation kinetics and the need to operate at higher pressures has directed the conceptual design towards a KrF

system. All of the scaling relationships derived in this report are based on KrF kinetics so that the results are far more concrete than if they had been done for XeCl. Should the operating efficiency of XeCl be improved such that it becomes favored over KrF, the optical design, pulsed power design and amplifier scaling techniques developed in this report for KrF could easily and rapidly be applied to possible XeCl laser amplifier systems.

3.2 Kinetic Modelling of KrF

3.2.1 E-beam Ionization and Excitation

Energy deposition rates for the e-beam have been calculated by a Monte Carlo electron transport program^{3.2-1} as a function of the gas mixture. Pressure, electron beam energy, and any associated magnetic field. The energy partitioning code predicts the fraction of energy distributed into the various ionic and excited states. In the pure e-beam excited system the energy removed from the primary electrons corresponds to 26.4 eV per ion pair for Ar and 24.1 eV per ion pair for Kr. Of this energy expended only 60 percent is required for ionization,

The remainder goes into excited state production,

and to kinetic energy of the secondary electrons. Since the rate for direct excitation of the electronic levels is about 28 percent of the ion-pair creation rate, we find about 10 percent of the e-beam energy going into excited state formation. The remaining 30 percent goes into secondary electron kinetic energy.

In this analysis the secondary electrons are assumed to be in a Maxwellian distribution defined by the electron temperature, T_e . This electron temperature represents a balance between the secondary electron energy input rate from the e-beam and the secondary electron energy loss rate due to electronic excitation, ionization, attachment and ion-electron recombination.

$$\frac{3}{2} k \frac{d}{dt} (N_e T_e) = 0.3E(\text{e-beam}) - E(\text{inelastic}) - E(\text{recombination})$$

where $E(\text{inelastic})$ includes excitation, ionization and attachment. Since these inelastic and recombination processes are a function of electron temperature, there exists a unique temperature defining the energy balance. The electron excitation, ionization and attachment rates used in this code are tabulated in Table A-1 of Appendix A.

3.2.2 Reaction Mechanisms and Radiative Properties

In an e-beam pumped laser mixture, consisting of Ar, Kr, and F_2 , the formation of KrF^* can proceed from the ion-ion recombination channel or from the neutral channel as shown in Figure 3.2-1. The neutral channel precursors (Ar^* and Kr^*) may be formed by ion-electron recombination and by direct and secondary electron excitation. The ion-ion channel forms KrF directly or indirectly from ArF^* which rapidly exchanges Kr with Ar to form KrF^* .

Quenching of the KrF^* is dominated by F_2 and electrons for bimolecular processes. At very high pumping rates the electron quenching rate will dominate, whereas at low pumping rates and high pressures the termolecular processes will dominate. Table 3.2-1 shows the formation and quenching fractions for a typical KrF^* mixture. It is seen that over 80 percent of the KrF^* is formed through the ion-ion (direct or indirect) channels and that 50 percent of the non-radiative quenching is due to three-body quenching. A complete list of the reaction mechanisms and radiative properties can be found in Table A-2 of Appendix A.

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Figure 3.2-1. Major Formation and Quenching Kinetics for Typical KrF* Laser Mixture.

Table 3.2-1

Formation and Quenching Fraction for a
Typical KrF* Laser Mixture

Ar/Kr/F ₂ = 95.8/4.0/0.2	
P = 1.7 atm	
P _{eb} = 220 kW/cm ³	
FORMATION	QUENCHING
$\text{Kr}^+ + \text{F}^- \xrightarrow{22\%}$ $\text{Kr}_2^+ + \text{F}^- \xrightarrow{13\%}$ $\text{ArKr}^+ + \text{F}^- \xrightarrow{11.2\%}$ $\text{ArF}^* + \text{Kr} \xrightarrow{27\%}$ $\text{Ar}_2\text{F}^* + \text{Kr} \xrightarrow{6\%}$ $\text{Kr}^* + \text{F}_2 \xrightarrow{19\%}$ $\text{ArKr}^* + \text{F}_2 \xrightarrow{1.2\%}$ $\text{Kr}_2^* + \text{F}_2 \xrightarrow{0.6\%}$	$\left\{ \begin{array}{l} + \text{Ar, Kr} \xrightarrow{8.5\%} \\ + 2\text{Ar} \xrightarrow{26\%} \\ + \text{F}_2 \xrightarrow{9.5\%} \\ + h\nu \xrightarrow{32\%} \\ + e^- \xrightarrow{24\%} \end{array} \right.$

3.2.3 Temperature Dependent Rates

In the KrF* laser, the three-body collisional quenching process $\text{KrF}^* + \text{R} + \text{R} \rightarrow \text{RKrF}^* + \text{R}$ ($\text{R} = \text{Ar}, \text{Kr}$) leads to decreased laser power and extraction efficiency.^{3.2-2} This occurs for two reasons. First, the quenching leads to a reduced small signal gain, and second, a direct product of this three-body quenching is an absorber, Kr_2F^* . The rate for this quenching process decreases as the temperature increases so that the efficiency of the KrF laser may improve as the temperature increases.

Shui^{3.2-3} has calculated the temperature dependence of the three-body quenching rate and we have included the possibility of turning this temperature dependence on or off in the MSNW kinetics code. In the scaling that we have carried out this temperature dependence does not give significantly different results from the nontemperature-dependent case. However, no scaling was done where the initial temperature of the amplifier had been elevated prior to e-beam pumping.

3.2.4 Code Predictions Compared with Experiments

In this section, a comparison is made between the code predictions and measurements of gain, loss, F_2 burn up, fluorescence efficiency and oscillator output power for a wide range of operating pressures and pumping rates. These comparisons were made using the code without the temperature dependent rates included for 3 body quenching.

Gain and Absorption: The KrF code was used to calculate the gain and absorption coefficients for the experimental measurement by Hawryluk, et al.^{3.2-2} Results for e-beam currents of 1.5 and 6 amps/cm², which correspond to power input rates of = 45 kW/cc and 180 kW/cc, for the 3 atm pressure mixture of 6% Kr, 0.3% F_2 , 93.7% Ar, are compared with experimental data as shown in Table 3.2-2. Since the absorption cross section of Kr F^* is not known, its value was assumed to be 6×10^{-18} cm in order to bring the calculated absorption into agreement with the experimental absorption. Under this assumption, the major absorbers are F_2 , F^- , and Kr_2F^* .

A test of the validity in using this large cross section for Kr_2F^* ² is the increase in the absorption coefficient in going from the 1.5 amp/cm² case to the 6 amp/cm² case. Since the F_2 absorption is essentially constant,

Table 3.2-2 Comparison of Measured and Code Calculated Absorption

Species	σ , cm ²	Number Density		α , %/cm	
		1.5 A/cm ²	6A/cm ²	1.5 A/cm ²	6 A/cm ²
F ₂	1.5x10 ⁻²⁰	2.14x10 ¹⁷	2.1x10 ¹⁷	0.321	0.315
Kr ₂ ⁺	1.6x10 ⁻¹⁸	6.3x10 ¹³	8.2x10 ¹³	0.01	0.013
Ar ₂ ⁺	1.3x10 ⁻¹⁷	2.2x10 ¹²	8.2x10 ¹²	0.003	0.011
Kr ₂ F ⁺	6x10 ⁻¹⁸	1.23x10 ¹⁴	4.7x10 ¹⁴	0.074	0.282
Ar ⁺⁺ , Kr ⁺⁺	6x10 ⁻¹⁸	4x10 ¹¹	2.3x10 ¹²	0.0002	0.0014
F ⁻	5.6x10 ⁻¹⁸	8.5x10 ¹³	1.47x10 ¹⁴	0.048	0.082
ArKr ⁺	1.4x10 ⁻¹⁷	1.4x10 ¹³	3.93x10 ¹³	0.02	0.055
Calculated α - α_{F_2} - α_{ArKr^+}				0.135	0.39
Calculated α - α_{F_2}				0.155	0.444
Measured α - α_{F_2}				0.15	0.44

Ar/Kr/F₂ = 93.7/6/0.3

P = 3 atm

and the F^- absorption scales as the square root of the pumping rate, the three-fold increase in the absorption (after subtracting out the F_2 absorption) must be dominated by species whose concentration increases linearly with power deposition, as is the case with Kr_2F^* .

Hawryluk's measurement of the gain coefficient for the 1.5 amp/cm case results in $g_0 = 0.45 \pm 0.05$ %/cm, whereas the KrF code gives $g_0 = 0.42$ %/cm. It is interesting to note that the assumption of the KrF* C and B states being equally mixed is in agreement with the measured gain coefficient, since in the limit of high pressure, a single level model would predict a gain coefficient twice as large as that for a model with equal concentrations of the B and C states (for a given KrF* formation efficiency).

Fluorescence Efficiency: A further test of the KrF code is in the determination of the fluorescence efficiency as a function of mixture ratio and gas pressure. The main effect of increasing pressure is to decrease the fluorescence because the two-body and three-body KrF* quenching processes begin to dominate, and there is little effect of pressure on the KrF* formation processes.

The experimental fluorescence efficiency data as obtained by J. A. Mangano et al.^{3.2-4} is shown in Figure 3.2-2 along with the KrF* code predictions. It is seen that the code predicts the fluorescence efficiency very well in the 0.5 to 3 atm pressure range. An approximate expression for the fluorescence efficiency, developed in Reference 3.2-4 is:

$$\eta = \eta_m \left(\frac{k_{ex}\tau_2 N_{Kr}}{1 + k_{ex}\tau_2 N_{Kr}} \right) (1 + k_1\tau_1 N_{F_2} + k_2\tau_1 N_{Ar}^2 + k_3\tau_1 N_{Kr} N_{Ar})^{-1}$$

where k_{ex} = rate constant for $Kr + ArF^* \rightarrow KrF^* + Ar$

τ_2 = radiative lifetime of ArF^*

τ_1 = radiative lifetime of KrF^* (effective lifetime of C+B states)

η_m = maximum efficiency for producing KrF^*

Figure 3.2-2. MSNW KrF Code Matches Fluorescence Data.

This equation fits the experimental data to within 10 percent over the range investigated. Reference 3.2-2 gives a value for $\eta_m \approx 25$ percent, which is in agreement with the KrF code which predicts $\eta_m \approx 26$ percent.

In the KrF code which assumed equal concentrations of KrF B and C states, the above expression would appear somewhat differently

$$\eta = \eta_m \left(\frac{k_{ex} \tau_B N_{Kr}}{1 + k_{ex} \tau_B N_{Kr}} \right) [1 + 2k_1 \tau_B N_{F_2} + 2k_2 \tau_B N_{Ar}^2 + 2k_3 \tau_B N_{Kr} N_{Ar}]^{-1}$$

where τ_B is the KrF B state radiative lifetime. However, since the quenching rates were evaluated experimentally as the products $k\tau_1$ at large diluent pressures, the radiative lifetime τ_1 would be twice τ_B under the assumption of equal population of B and C states. Therefore, the quenching terms would be equivalent (e.g., $2k_1 \tau_B N_{F_2} = k_1 \tau_1 N_{F_2}$).

Fluorine Burnup: Data from F burnup experiments performed in Reference 3.2-5 for Argon/Fluorine mixtures were used to test the predictive capability of the KrF code. Although the gas mixtures did not contain Kr, the burnup rate of F_2 should not be altered by addition of a small percentage of Kr, since the major removal rates, neglecting the attachment rate, are due to the excited state reaction with F_2 . These rates are similar for Ar and Kr. The results of the KrF code are shown in Figure 3.2-3 where it is seen that the total burnup rate falls within the data over most of the energy input range. Also shown is the burnup due to attachment, which is essentially equal to the ion-pair production rate since the electron-ion recombination is small for the experimental mixture.

Power Extraction: The KrF* code was compared with the available experimental data for oscillator power extraction. A range of e-beam power input rates from ≈ 200 kW/cc to 7 MW/cc and a range of pressures from 1.5 to 3.3 atm is covered, and should provide a good test for the predictive capability of the code.

At the low power input level experiments at AVCO described in References 3.1-1 and 3.2-6, the code is in good agreement with the laser output energy although the $g_0 L$ and αL predicted by the code are about 10 percent lower than the experimental evaluation, as shown in Table 3.2-3.

Experiments performed at Lawrence Livermore Laboratories^{3.2-7} (LLL) for e-beam power input levels of 700 kW/cc and 60 nsec duration produced 4.6 J/l whereas the KrF* code predicts 4.7 J/l. After adjustment for losses due to the back mirror reflectivity, and "dead space" F_2 absorption, the outputs become

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Figure 3.2-3. MSNW KrF Code Results for F_2 Burn-up Compared to Experimental Measurements.

Table 3.2-3 Comparison of Laser Experiments with Code Predicted Results.

		$\eta(\%)$	J/ℓ	$g_0 L$	αL
AVCO 1 m 212 kW/cc 1.7 atm	CODE EXP'T.	9.3 9.0	12.4 12.0	3.4 3.7	0.29 0.35
SANDIA 40 cm 7 MW/cc 3.3 atm	CODE EXP'T.	11.7 11.4	41.0 40.0	8.0 7±1	0.48 0.4-0.6
LLL 50 cm 700 kW/cc 1.5 atm	CODE EXP'T.	12.8 12.6	5.4 5.3	4.3 --	0.24 --

5.3 J/ℓ and 5.4 J/ℓ respectively. For these short pulse lasers the buildup time of the cavity intensity from the background intensity (ϕ_0) due to spontaneous emission becomes important, as shown in Figure 3.2-4, where the ϕ_0 was varied 6 orders of magnitude. Assuming a background intensity of about 2 kW/cm² the kinetics code is seen to describe the output intensity pulse shape and width fairly accurately.

High intensity electron beam, short pulse KrF oscillator experiments were performed at Sandia by Tisone et al.^{3.2-8} For input pump rates of 7 MW/cm², they obtained a peak of 40 J/ℓ output energy with a laser efficiency of 11.4 percent, after correction for losses due to the cell windows, and "dead space" F₂ absorption. In addition, they estimated the small signal gain coefficient g_0 , the non-saturable absorption coefficient, α , and the saturation flux ϕ , by applying the Rigrod^{3.2-9} theory for output power as a function of reflectivity and absorption. However, because of the very high gain coefficients (≈ 20 %/cm). The Rigrod analysis can have a factor of two uncertainty as shown in Figure 3.2-5.

Applying the KrF code to the Sandia experimental conditions resulted in a peak output of 41 J/ℓ with an efficiency of 11.7 percent, after adjustment for losses due to the cell windows, and "dead space" F₂ absorption. The average output power was calculated for two effective reflectivities and is in good agreement with the data as shown in Figure 3.2-5.

The kinetics code has been shown to reproduce measurements of gain, loss, F₂ burn-up, fluorescence efficiency and the time history of oscillator power extraction over a wide range of operating conditions. Because of this success, the code has been used with a certain amount of confidence in the scaling of the amplifier in the KrF conceptual design. Nevertheless, uncertainties do exist in the kinetics, that should be resolved, and they are discussed in the next section.

Figure 3.2-4. MSNW KrF Code Matches the Time History of Oscillator Output Power Measurements.

Figure 3.2-5. MSNW KrF Code Predictions of Oscillator Power Output Compared with Sandia Measurements.

3.2.5 Key Areas of Uncertainty in KrF* Laser Kinetics

Kinetic Formation Paths within the KrF* Manifold: Figure 3.2-6 shows the kinetic formation path within the KrF* manifold. KrF* molecules are produced in the C, B(v=0, 1) and B(v>1) states due to e-beam pumping and there is a finite rate for vibrational relaxation from the v > 1 state to the v = 0 and 1 levels. In addition, there is a finite rate for the exchange between the B and C states. In all of our work on the KrF conceptual design, we have taken these rates to be large and denote this limit as the equilibrium condition. In this section we examine the effect of using finite rates for k_{vib} and k_{ex} on the amplifier operation.

In Figure 3.2-7 a comparison is made between the KrF code and the Sandia experiment. The ratio of output power, predicted by the code, is normalized to the experimental result and this ratio is plotted versus k_{vib} for three values of k_{ex} (5×10^{-11} , 10^{-10} and 10^{-9} cm³/sec). It is clear that the experimental results are not reproduced unless $k_{vib} \geq 10^{-10}$ cm³/sec; Above this value of k_{vib} the code result does not change by more than $\pm 10\%$ over the range of k_{ex} values examined. In Figure 3.2-8 the effect of a finite B-C exchange rate and vibrational relaxation rate on the overall KrF efficiency is studied. Here we plot efficiency versus cavity flux in MW/cm² using the 10^{-10} cm³/sec value for k_{vib} ; k_{ex} was set equal to 10^{-10} and 10^{-9} cm³/sec. Also included is an equilibrium curve. From this plot it can be seen that the effect of finite rate constants in the KrF* manifold is to reduce the laser efficiency by about 10 percent. Because these effects are small, they have been neglected in our analysis and scaling development for the conceptual design. Accurate values for these rates should, eventually, be determined.

Cross Sections and Rate Constants: The Kr₂F* absorption was set in the MSNW Code by matching the code predictions to experimental measurements of gain and absorption made at two pumping rates. This cross section should be determined independent of the kinetics code. Further, the exchange rate for $ArF^* + Kr \rightleftharpoons KrF^* + Ar$ must be studied.

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Figure 3.2-5. Model of Kinetic Formation Paths within the KrF* Manifold.

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Figure 3.2-7. Effect of Finite Vibrational Relaxation and B-C Exchange Rates in the MSNW KrF Code.

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Figure 3.2-8. Effect of KrF Vibrational Relaxation and B-C Exchange on Amplifier Efficiency.

Temperature Dependent Rates: As previously mentioned, the temperature dependence of 3-body quenching rates will impact the overall laser efficiency and this dependence must be determined. Also, the temperature dependence of the F_2 attachment rate should be examined.

3.3 Extraction Efficiency: Nonsaturable Absorption

3.3.1 Single Pass Extraction

In the absence of intrinsic absorption, the extraction efficiency (the ratio of extracted energy to that available for extraction) approaches unity as the gain length product is increased. This is not true when a nonsaturable absorber is present. In this case the photon flux equation is written as

$$\frac{1}{\beta \pm} \frac{d\beta \pm}{dx} = \pm \frac{g_0}{(1 + \beta^+ + \beta^-)} - \alpha_{NS} \quad (3.3-1)$$

where β is the coherent photon flux normalized to the saturation flux, g_0 is the small signal gain minus any saturable absorption coefficients and α_{NS} is the nonsaturable absorption coefficient. Because of the nonsaturable loss, the extraction efficiency has a maximum near a loss length, $\alpha_{NS}L$, of order unity and thereafter decreases rapidly. This behavior can be demonstrated by integrating the flux equation (3.3-1) for a single pass amplifier of length L to obtain

$$\left[\frac{\beta(\text{out})}{1 - \frac{\beta(\text{out})}{(\gamma-1)}} \right]^\gamma = \left[\frac{\beta(\text{in})}{1 - \frac{\beta(\text{in})}{(\gamma-1)}} \right]^\gamma \exp \left[\left(1 - \frac{1}{\gamma} \right) g_0 L \right] \quad (3.3-2)$$

where γ is the gain to loss ratio defined as (g_0/α_{NS}) , and $\beta(\text{in})$ is the normalized input flux at $x = 0$. The extraction efficiency is defined as

$$\eta_{\text{ext}} \equiv \frac{B(\text{out}) - S(\text{in})}{g_0 L}, \quad (3.3-3)$$

so that the flux equation (3.3-2) can be used to compute the extraction efficiency from Equation (3.3-3) versus the amplifier gain length product for a given input flux. Figure 3.3-1 shows the results for gain-to-loss ratios of 10 and 20. The effect of the nonsaturable absorption coefficient is apparent, and shows that the optimum gain length decreases as the gain-to-loss ratio is reduced and clearly illustrates the amplifier length limitation.

The power amplifiers studied in this program have large gain length products ($g_0 L \sim 10$) and small aspect ratios (length to diameter ratios of order unity). This situation demands that the amplifier gain be well saturated if we are to suppress possible parasitic oscillations and the generation of ASE flux that would degrade the extraction efficiency. For a single pass amplifier, the input flux would have to be greater than a saturation flux to adequately load the gain at the entrance aperture and this would severely limit the available power gain. In order to increase the power gain while saturating the medium, it is necessary to have multiple pass extraction from the system.

3.3.2 Double Pass Extraction

Rigrod^{3.3-1} and more recently, Eimerl^{3.3-2} have considered the two pass amplifier characteristics with uniformly distributed nonsaturable loss. Their results for a two pass amplifier (100 percent reflection on one end and 100 percent output coupling on the other) can be summarized as

$$a_{NS} L + \ln[S(\text{out})/S(\text{in})] = A \ln \left\{ F[B(\text{out}) / F B(\text{in})] \right\} \quad (3.3-4)$$

$$\text{where } A = \gamma / \sqrt{(\gamma-1)^2 - (2\epsilon_0)^2}, \quad (3.3-5)$$

$$F(\epsilon) = \frac{\sqrt{(\gamma-1)^2 - (2\epsilon_0)^2} + (\gamma-1-2\epsilon)}{\sqrt{(\gamma-1)^2 - (2\epsilon_0)^2} - (\gamma-1-2\epsilon)} \quad (3.3-6)$$

Figure 3.3-1. Optical Extraction Efficiency in Single Pass Amplifier.

and

$$\beta_o^2 = \beta(\text{in}) \cdot \beta(\text{out}) \quad (3.3-7)$$

Solving these expressions numerically, one obtains the extraction efficiency for the two pass amplifier with nonsaturating loss; the extraction efficiency is plotted in Figure 3.3-2 versus the gain length for anticipated values of the gain-to-loss ratio γ . Just as in the single pass system, there is an optimum gain length for maximum extraction efficiency. By comparing Figure 3.3-1 and 3.3-2 it is seen that the optimum gain length is approximately the same for the single and double pass systems for the same gain-to-loss ratio and low input flux, $\beta(\text{in})$. This reflects the fact that in the double pass system only the second pass saturates the gain medium when the input flux is low.

The purpose of the double pass amplifier is to generate a more uniformly saturated gain medium; in Figure 3.3-3 we plot the resulting local normalized flux in the single and double amplifier for comparison. For this plot the input flux for both single and double pass amplification was $\beta(\text{in}) = 0.1$. It is clear that the double pass amplifier leads to a more uniformly saturated gain distribution. However, even in the two pass system, there is a lower bound to the input flux in order to saturate the medium at the reflection end of the amplifier. We conclude that the optimum amplifier configuration would have at least two and possibly more passes to adequately saturate the gain medium; or, for an amplifier with a given number of passes, there will be a maximum power gain below which the parasitic and ASE problems will be acceptable. From these results, it is possible to obtain the optimum extraction efficiency versus the gain-to-loss ratio for a variety of input flux levels. These results are presented in Figure 3.3-4 along with the normalized output flux, $\beta(\text{out})$. From this figure we note that the maximum achievable extraction efficiency falls between 0.5 and 0.55 if the gain-to-loss ratio is 15 and the input flux falls between 0.03 and 0.3 times the saturation flux.

The design of each amplifier will be a compromise between the requirement to maximize the intrinsic efficiency of the unit and the

Figure 3.3-2. Optical Extraction Efficiency in Double Pass Amplifier with Nonsaturable Absorption.

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Figure 3.3-3. Flux Distribution in Single and Double Pass Amplifiers with Nonsaturable Loss.

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Figure 3.3-4. Maximum Extraction Efficiency in a Double Pass Amplifier with Nonsaturable Loss.

practical limitations imposed upon the e-beam design limits and the optical damage limits. To carry out a parametric design analysis, one might proceed as follows: For a given e-beam pulse length, τ_p , there is a corresponding pump power density, P given by

$$P(w/cc) = \frac{\epsilon(J/cc)}{\eta_{prod} \tau_p} \quad (3.3-8)$$

where ϵ is the stored energy density. It will be shown that ϵ depends only on the total amount of F_2 in the amplifier and only weakly on the pumping rate. Consequently, once the gas mix and pressure is chosen, we can define a pumping power, P , after picking a run time using Equation (3.3-8). The values of g_0 and γ are then found from the steady state kinetics, and an optimum extraction efficiency, η_{ext} , and gain length product, $g_0 L$, can then be obtained as a function of the input flux. One can then determine the optimum amplifier length and corresponding cross sectional area for a specified output energy which is given by

$$w^2 = \frac{E_{out}}{L \eta_{ext}} \quad (3.3-9)$$

A trade-off analysis must then be done between the gas mixture, the pressure, the input flux, the e-beam scaling limitations, optical damage limits and the amount of unextractable volume due to angular multiplexing.

Before attempting such a trade-off, we note that the power gain in the amplifier depends primarily upon the input flux and only weakly on the gain-to-loss ratio, while the maximum extraction efficiency and the output flux are fairly insensitive to the input flux. The choice of the input flux is really determined by the degree of gain loading required to suppress parasitic oscillations and the effects of ASE. Consequently, before amplifier scaling is addressed further,

an analysis of the effects of ASE on the extraction efficiency and the limits imposed on the power gain by the requirement to suppress parasitics is now developed.

3.4 Amplified Spontaneous Emission (ASE)

For the RgH conceptual design, the requirement of large volume and large apertures is in conflict with the necessity to minimize ASE which may result from the inherent high value of small signal gain for these systems. Therefore, it is necessary to calculate the effects of ASE on the extraction efficiency before amplifier scaling can be initiated.

In order to compute the effects of ASE on the extraction efficiency, an expression must be identified that expresses the ASE flux at any point in the amplifier; further, this flux must be incorporated into the excited state rate equations in such a way as to de-populate the upper state and rob the amplifier of gain and extractable energy. The excited state rate equation for the KrF RgH laser amplifier may be written as

$$\frac{dN^*}{dt} = R - \frac{N^*}{\tau_u} - \frac{gI_c}{h\nu} - (\text{ASE TERM}). \quad (3.4-1)$$

where N^* is the upper state density in cm^{-3} , τ_u is the upper state lifetime, g is the gain coefficient in cm^{-1} , I_c is the coherent flux, and R is the upper state pumping rate.

It should be clear that the ASE term must have the form $(gI_{\text{ASE}})/h\nu$ where I_{ASE} is the ASE flux at the point of interest. In steady state, $dn^*/dt = 0$, and in the small signal limit, $I_c = I_{\text{ASE}} = 0$, so that one finds

$$R = \frac{N_o^*}{\tau_u} \equiv \frac{g_o}{c\tau_u}. \quad (3.4-2)$$

Using this expression for the pumping rate, one obtains the usual gain loading expression with the ASE flux added to the coherent flux

$$g = \frac{g_0}{1 + \left(\frac{I_c + I_{ASE}}{I_{sat}} \right)} \quad (3.4-3)$$

where $I_{sat} \equiv \frac{h\nu}{\sigma\tau_u}$.

An expression for the ASE flux can be formulated in the following way: The number of spontaneously emitted photons from the point (xyz) per unit volume is

$$\frac{N^*(x,y,z)}{\tau_R} \quad (3.4-4)$$

where τ_R is the spontaneous radiation lifetime.. Assuming these photons are emitted isotropically, the flux seen at (x_0, y_0, z_0) a distance

$$|r| = \sqrt{(x-x_0)^2 + (y-y_0)^2 + (z-z_0)^2}$$

from the emitting volume element dV is just

$$\frac{h\nu N^*(x,y,z) dV}{\tau_R 4\pi |r|^2} \quad (3.4-5)$$

If these photons experience gain as they propagate over the distance $|r|$ then the flux due to spontaneous emission at (x_0, y_0, z_0) becomes

$$dI_{ASE}(x_0, y_0, z_0) = \frac{h\nu}{\tau_R} \frac{N^*(xyz) \exp \left\{ \int [g(x'y'z') - \alpha_{NS}] d\ell' \right\}}{4\pi |r|^2} dV \quad (3.4-6)$$

where the line integral in the exponential is taken over a path from the emitting volume to the observation point. This geometry is illustrated in Figure 3.4-1. To obtain the total ASE flux, Equation (3.4-6) is integrated over the active amplifier volume. In this expression, we have neglected the photon transit time from (xyz) to (x_0, y_0, z_0) and this implies that the gain distribution within the amplifier does not change significantly within a ~10 nsec time interval. By definition, this condition will be satisfied in a steady state analysis. Using

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Figure 3.4-1. Geometry Used in Computing the ASE Flux

$I_{\text{sat}} = h\nu/\sigma \tau_u$ and $g = N^*\sigma$ the normalized ASE flux can be written as

$$\beta_{\text{ASE}} = \frac{I_{\text{ASE}}}{I_{\text{sat}}} = \frac{\tau_u}{\tau_R} \int \frac{g(xyz) \exp \left\{ \left| \int [g(x'y'z') - \alpha_{\text{NS}}] d\ell' \right| \right\} dV}{4\pi |\vec{r}|^2} \quad (3.4-7)$$

From Equation (3.4-3) we note that the saturated gain in the amplifier can be written in the form

$$g = \frac{g_0}{(1 + \beta_c + \beta_{\text{ASE}})} \quad (3.4-8)$$

and the coherent photon transport equation can be expressed as

$$d\beta_c = \beta_c (g - \alpha_{\text{NS}}) dz \quad (3.4-9)$$

It is apparent that the loaded gain distribution determines the ASE flux and coherent flux; however, both of these photon fluxes also reimpact the gain distribution. As a result, in order to solve Equations (3.4-7) (3.4-8), and (3.4-9), we choose an initial gain distribution and then compute both $\beta_c(x,y,z)$ and $\beta_{\text{ASE}}(x,y,z)$. These values of flux are then used to redefine the loaded gain and a new set of flux levels are then generated. This process is repeated numerically until a self consistent set of flux and loaded gain distributions are generated. The actual evaluation of β_{ASE} has been carried out in a cylindrical coordinate system and this process is described in detail in Appendix B.

In our analysis, the spectral width of the ASE flux has been neglected, it has been assumed that all spontaneously emitted photons are created at the center of the gain line. As a result our calculations of extraction efficiency are conservative and represent a worst case. In Appendix C we consider how the spectral width of the ASE flux could be taken into account in these calculations and then estimate the increase in the extraction efficiency that may result when this is carried out. For the present we use the conservative results and note that the extraction efficiencies presented will not increase by more than 10 - 20 percent due to this effect.

3.4.1 Extraction Efficiencies with ASE

In order to understand the effects of ASE on extraction efficiency, a set of data has been generated for single and double pass amplifiers operating with gain-to-loss ratios of 10 and 20. For most of the cases considered, the amplifier aspect ratio was set equal to unity; this was done because a large amplifier (≥ 50 kJ) will require an aperture ≥ 100 cm on a side to avoid damage and it is unlikely that the amplifier length will be much larger than its width in order to avoid generating excessive values of $g_0 L$ at a reasonable pulse length. In all cases considered, the ratio of τ_u/τ_r was set equal to 0.33; the kinetics predicts this ratio to be 0.3 to 0.45 for nearly all amplifier runs studied. This ratio begins to drop when pumping rates exceed 1000 kW/cc so that the ratio used represents a good approximation for most pumping rates and a worst case for very large pumping rates.

Figure 3.4-2 shows results for a single and double pass amplifier. The extraction efficiency is plotted versus $g_0 L$, and curves with and without ASE effects are shown. The input flux, $\beta_c(\text{in})$, was 0.1 times the saturation flux in both cases. It is clear that the single pass amplifier with this input flux loading is completely inadequate as the extraction efficiency drops from 40 to 15 percent due to ASE. The double pass amplifier is rather improved but still gives only 30 to 40 percent extraction depending on the gain-to-loss ratio. In Figure 3.4-3 the same data is plotted in a different way. The extraction is shown versus the input flux for the single and double pass amplifier. The gain length, $g_0 L$, was fixed at 10. Again, it is clear that the double pass amplifier has superior extraction efficiency at low and moderate input flux levels. From the double pass results, it appears that extraction efficiencies of 30 to 50 percent are possible, depending on the gain-to-loss ratio, and an input flux of 0.3 times the saturation flux. An amplifier built with these properties would have a power gain of ≈ 15 . Thus, a tentative conclusion is that a double pass amplifier must have moderate power gains if its extraction efficiency is to remain high. A single pass amplifier is out of the question because its power gain would

Figure 3.4-2. Extraction Efficiencies for Single and Double Pass Amplifiers
Versus $g_0 L$ with and without ASE.

Figure 3.4-3. Extraction Efficiencies for Single and Double Pass Amplifiers versus the Input Flux, $\beta(in)$.

be only 3 - 4 at extraction efficiencies of 35 percent; additionally, its gain loading would be very nonuniform. The next figure, Figure 3.4-4, illustrates the extraction efficiency dependence on the amplifier aspect ratio with a g_0L of 10. It should be noted that for the lower input flux, $B(\text{in}) = 0.1$, the extraction efficiency drops rapidly as the aspect ratio falls below unity and increases slowly above unity. Therefore, one should stay away from low aspect ratios but there is not too much to be gained from going to aspect ratios much greater than unity either. At high input flux levels, the lower aspect ratios become more acceptable which could be an advantage in terms of reducing the unextracted volume due to angular multiplexing.

The next two figures show the local flux and loaded gain distribution in the single and double pass amplifiers with and without ASE effects. Figure 3.4-5 presents the results for the case without ASE. The very nonuniform gain distribution for the single pass amplifier is apparent and the gain is essentially unloaded at the entrance aperture. The double pass amplifier develops a much more uniform gain distribution where the loaded gain varies between $1/5$ and $1/3$ of the unloaded value. The extraction efficiency for the single and double pass amplifier are comparable, 46 and 58 percent respectively, without the influence of ASE. Figure 3.4-6 shows the same amplifier configurations with the ASE flux included. For the single pass amplifier the loaded gain distribution is quite uniform but this is because of the very large ASE flux (nearly 2.0 saturation fluxes) that has reduced the extraction efficiency to 16 percent. The ASE effect is greatly reduced in the double pass amplifier, as we can see by the fact that the extraction efficiency remains at a respectable 45 percent.

One can conclude from these results that large amplifiers will have moderate to low power gains (10 to 30) and aspect ratios near unity, and will require multiple pass extraction optics to maintain acceptable extraction efficiency.

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Figure 3.4-4. Extraction Efficiency versus the Amplifier Aspect Ratio.

Figure 3.4-5. Local Flux and Gain Distributions in Single and Double Pass Amplifiers without ASE Effects Included.

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Figure 3.4-5. Local Flux and Gain Distribution in Single and Double Pass Amplifiers with ASE Effects Included.

The computer code developed to calculate extraction efficiency in the presence of ASE was up-graded during the program to include the effect of a large mirror in the amplifier. The mirror increases the ASE flux seen at any observation point in the gain medium because ASE photons are seen directly from other volume elements and also through a reflection from the mirror surface. Figure 3.4-7 illustrates this effect and shows that the mirror essentially forms a mirror image of the amplifier to the right. We use this fact to advantage in the code by computing the ASE flux from the amplifier itself and then from the mirror image amplifier.

The effect of the mirror is shown in Figure 3.4-8 where a plot of the extraction efficiency is shown versus the gain-to-loss ratio for double pass amplifiers. Three plots are shown for input flux levels of 0.1, 0.3 and 0.5 times the saturation flux. For each case the extraction efficiency is shown: (a) without any ASE effects, (b) ASE effects included with and without a large mirror used for double passing the coherent beam. With an input flux of 0.3 times the saturation flux, the reduction in the extraction efficiency, due to the mirror, is increased by nearly 50 percent. Thus, this effect should be included in any analysis of the amplifier scaling. Consequently, a set of data was generated with the large mirror included and is presented in Figure 3.4-9 where the extraction efficiency is plotted versus the gain-to-loss ratio for a variety of g_0L values. Plotting the results in this format turns out to be very useful when the extraction calculations are integrated with the KrF kinetic results. This will become clear in the following sections. However, to further illustrate the range of power gains accessible, we plot power gain versus output flux in Figure 3.4-10. On this plot, lines of constant η_{ext} , g_0L and $B(\text{in})$ are shown and it becomes obvious that power gains of 10 to 15 are the maximum possible if extraction efficiencies greater than 40 percent are desired. Further, to keep $g_0L \leq 8-10$ and the power gain greater than 10, the input flux must be ~ 0.3 times the saturation flux. Before turning to the kinetic code results and amplifier scaling, an analysis of parasitic limitations on the amplifier design is presented.

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Figure 3.4-7. Geometry Illustrating the Increased ASE Flux due to a Large Mirror Used to Double Pass the Amplifier.

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Figure 3.4-8. The Reduction in Extraction Efficiency Caused by Reflection of ASE Flux by a Large Mirror Used for Multiple Pass Extraction is Illustrated in These plots of η_{ext} versus the Gain/Loss Ratio.

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Figure 3.4-9. Extraction Efficiency versus the Gain-to-Loss Ratio for a Variety of $g_0 L$ and $\beta(\text{in})$ Values. This figure summarizes the results of extraction efficiency calculations where ASE effects have been included.

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Figure 3.4-10. Operating Map Based on Extraction Efficiency Computations.

3.5 Parasitic Limitations

Considerable care will be needed in the design of a large amplifier to insure that parasitic oscillations do not build up during the initial start-up of the amplifier and during normal gain loaded operation. In order to estimate the maximum wall reflectivity that can be tolerated, a parametric plot of the maximum loaded gain (normalized to the small signal gain) has been constructed from the extraction code results. This plot is presented in Figure 3.5-1 where the normalized loaded gain, g/g_0 , has been plotted versus the normalized input flux, $\beta(\text{in})$, for g_0L values ranging from 6 to 14. The aspect ratio for these amplifiers is unity so the transverse gain length product is the same as g_0L . For input flux levels of 0.1 to 0.3 the small signal gain is seen to be reduced by factors of 2.5 to 4.0, and this reduction is essential if transverse parasitic oscillations are to be suppressed. A simple model for setting limits on the wall reflectivity can be constructed by assuming specularly reflecting walls of reflectivity R . The condition to be met, if parasitics are to be kept below threshold, is

$$R^2 \exp \{ 2g_0L[g/g_0 - 1/\gamma] \} < 1 \quad (3.5.1)$$

for a round trip pass transverse to the coherent beam. Using this expression and the loaded gain results from the previous figure, one can set an upper bound on the wall reflectivity versus g_0L for various input flux levels. Figure 3.5-2 presents the results of carrying out this process; for an amplifier operating at $g_0L = 8$ and an input flux of 0.2, the wall reflectivity must be below 15 percent to suppress transverse parasitic oscillation. Superimposed on this figure is a lower bound line that one must remain above in order to achieve an extraction efficiency ≥ 40 percent.

In order to discriminate against parasitics, the wall reflectivities must be reduced as much as possible; the surfaces must be diffusive reflectors whenever possible and the e-beam foils may need to be antireflection coated. In addition, baffles may be needed at the e-beam foil ribs in

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Figure 3.5-1. Maximum Value of the Amplifier Loaded Gain versus the Input Flux.

DOUBLE PASS AMPLIFIER
GAIN TO LOSS = 15

$$R^2 \exp[2g_0 L (g/g_0 - 1/\gamma)] < 1.0$$

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Figure 3.5-2. Maximum Allowable Wall Reflectivity versus $g_0 L$ for a Variety of Input Flux Levels. At a given $g_0 L$, one must remain below the solid curves to prevent parasitic oscillations. The broken line represents the 40 percent extraction efficiency bound.

order to suppress glancing angle reflections.

One additional technique that has been considered is the use of an absorbing gas curtain or "soft wall" adjacent to the e-beam foils. Kinetic code calculations show that a mixture of Ar and F_2 can reduce a parasitic intensity by a factor of 2 or more and may be an important technique in an amplifier design. Figure 3.5-3 illustrates a possible gas curtain configuration where the absorbing gas is introduced over the foil surface through a distribution network in the foil support structure. In this example the optical pulses occur with a 10 Hz repetition rate and the cavity flow is adjusted to provide two changes of cavity gas between each pulse. For a 100 cm cavity the flow is 2000 cm/sec. A fluid dynamic analysis (presented in Appendix D) of this configuration shows that the turbulent shear layer will grow to a width of 27 cm and that the foil will have to be recessed 17 cm to keep the shear layer from impacting the foil. A mass flow of 1550 gm/sec of absorbing gas will be required. In Appendix D other possible curtain flow geometries are considered including cavity flow with curtain flow, curtain flow without cavity flow and a quiescent cavity with a segmented curtain. Table 3.5-1 lists the results for each of these possible configurations.

3.6 Scaling Maps for Large KrF Amplifiers

During this conceptual design the KrF kinetics code has been run for a variety of operating conditions including: 200 kw/cc to 1000 kw/cc pumping rates, 1 and 2 atm pressures, a wide range of F_2 mixtures, and with and without temperature dependent rates included (refer to Section 3.1). For this multitude of cases the gain-to-loss ratios, small signal gain and saturation flux were tabulated as a function of run time. Using these results the extraction efficiency was computed throughout the run time for each case where the input flux was assumed to be 0.3 times the saturation flux. In every case studied, it was found that the extraction efficiency is very nearly constant during the run times considered. This occurs because during the run the gain to loss ratio is, in general, falling, but so is the value of g_0 or g_{0L} . Recall, from Figure 3.4-9 (where a plot of extraction efficiency versus gain to loss ratio was presented) that the extraction efficiency decreases as the gain to loss

INJECTION OF Ar/F₂ MIXTURE
INTO WALLBOUNDARY LAYER

TURBULENT SHEAR AND MIXING LAYER
3.2 cm THICK FOR 200 cm APERTURE
40 m/sec FLOW VELOCITY

280 kW REQUIRED TO SEPARATE MIXED
GASES FOR RECYCLING
(TWO FOILS)

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Figure 3.5-3. Possible Geometry for an Absorbing Gas Curtain for Parasitic Control.

TABLE 3.5-1 ABSORBING CURTAIN FLOW PARAMETERS FOR A 100cm x 100cm E-BEAM FOIL APERTURE

Configuration	Upstream Foil Recesses h_1 (cm)	Downstream Scoop Height h_2 (cm)	Cavity Flow Rate U_1 (cm/sec)	Absorbing Gas Flow Rate U_2 (cm/sec)	Mass Flow (g/sec)
Cavity Flow, Quiescent Curtain	17	27	2000	0	1550
Cavity Flow, Curtain Flow	3.11	6.22	2000	2000	1120
Quiescent Cavity, Curtain Flow	11.1	26.7	0	114	227
Quiescent Cavity Flow, 10cm Segmented Curtain Flow	1.1	2.67	0	1140	2270

ratio decreases but increases as $g_0 L$ decreases. These two effects nearly balance leaving the extraction efficiency constant during the pump time. This effect is illustrated in Figure 3.6-1 where the gain to loss ratio, the gain length product and the percent F_2 "burned up" are plotted versus the run time for a 1 atm. 300 kw/cc, 0.5% F_2 case. In this example, the run time was terminated after 50 percent of the F_2 had been "burned up".

Notice that the gain-to-loss ratio and gain length product are both dropping in time and this results in a constant extraction efficiency during the run as shown in Figure 3.6-2 where η_{ext} and $g_0 I_{sat}$ are plotted versus the run time. Not only is the extraction efficiency essentially constant but the stored power density is constant as well. This result simplifies the scaling of the final amplifier because one can compute η_{ext} and $g_0 I_s$ at any time during the run and use the value for the entire run time.

The next step in comparing the many possible operating conditions was to compute the stored energy by assuming a particular run time. As an example of this comparison, the results for run times set by 50 percent F_2 "burn up" are presented. Figure 3.6-3 shows a plot of the stored energy versus the product of [percent F_2] x [pressure (atm)] or total F_2 content per unit volume. The data points fall on a straight line over the range of F_2 mixtures and pressures studied. The points on this line have pumping rates varying from 200 kw/cc to 1000 kw/cc and run times between 150 nsec and 1000 nsec. This result is expressed as

$$\epsilon(\text{J/cc}) = k_1 [\% F_2] [\text{pressure (atm)}] \quad (3.6.1)$$

where k_1 is a constant and $\epsilon(\text{J/cc})$ is the stored energy per unit volume.

It should now be clear that the physical size (volume) of a large amplifier of fixed energy output depends only on the total amount of F_2 per unit volume and the extraction efficiency; the run time is then a function of the pumping rate and also the F_2 content. However, for each value of run time, and pump rate the amplifier operating $g_0 L$, pulsed power requirements, and extraction efficiency may be very different. It is the purpose

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Figure 3.6-1. Time Dependent Parameters for an Amplifier Operating at 1 atm with 0.5% F₂, and Pumped at 300 kW/cc.

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Figure 3.6-2. Typical Time Dependence of the Extraction Efficiency and Amplifier Stored Power Density. These results are for a 1 atm amplifier operated with 0.5% F_2 and pumped at 300 kw/cc.

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Figure 3.6-3. Extractable Energy Density for Amplifiers Operating at 1 and 2 atm, and Over a Wide Range of Pumping Rates and Run Times.

of this section to address these issues as part of the final amplifier scaling.

As expected, the run time, until 50 percent of the F_2 is "burned up," is approximately inversely proportional to the pump rate and directly proportional to the F_2 content.

$$T_p(\text{nsec}) = k_2 [\%F_2] [\text{Pressure(atm)}]/[\text{Pump (kw/cc)}]. \quad (3.6.2)$$

Here k_2 is a constant and T_p is the amplifier run time. In addition, one can write the total energy extracted in terms of the amplifier volume, w^2L , the extraction efficiency, η_{ext} , and $\epsilon(\text{J/cc})$.

$$E_{\text{out}} = w^2 L \eta_{\text{ext}} \epsilon(\text{J/cc}) \quad (3.6.3)$$

These simple relationships suggest a useful format for the final amplifier scaling maps. If one plots the amplifier run time versus the pumping rate on log-log paper the result is a straight line; further, if one increases the amount of F_2 , a new straight line can be drawn above the other because the run time is roughly proportional to the F_2 content. Since the F_2 content determines the total stored energy, the size of the amplifier needed to deliver a certain total energy is fixed along each line once the extraction efficiency is known.

As an example, we show these resulting plots for a 50 kJ amplifier with an assumed extraction efficiency of 40 percent. (Using the same extraction efficiency for the entire map will be justified shortly.) Figures 3.6-4 (a) and (b) present these plots for 1 and 2 atm operating pressures, respectively. To the right of each line is the percent F_2 used and the resulting extracted energy per unit volume in joules/liter where a 40 percent extraction efficiency has been assumed. To the left of each line, the total volume, in liters, needed to obtain 50 kJ is indicated.

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Figure 3.6-4. Scaling Map Format for 1 and 2 atm Pressure Amplifiers.

To carry this analysis further, one must determine the extraction efficiency for all points on these plots. In order to do this, the aspect ratio, L/W , for the amplifier must be chosen and this must be done consistent with damage threshold conditions at the aperture, and good extraction efficiency. As an example, we consider an aspect ratio of unity and this fixes the dimensions of the amplifier. The extraction efficiency associated with each point on the plot can now be computed (recall that the extraction efficiency remains nearly constant during the pulse until 50 percent of the F_2 is "burned up"). Extraction efficiencies were calculated for operating points on these plots by taking the kinetic code results for the g_0L and the gain to loss ratio and merging them with the extraction code results that include the effects of ASE and that are summarized in Figure 3.4-9. For this example, the input flux was chosen to be 0.3 times the saturation flux.

The resulting extraction efficiencies have been added to the scaling maps in Figures 3.6-5(a) and 3.6-5(b) and are enclosed in small circles for easy identification. The remarkable thing about these plots is that the extraction efficiency is essentially constant over the entire map, or at least over the range of interest. This occurs for the same reasons that the extraction efficiency remains constant during a particular run; both the g_0L and gain-to-loss ratio change in a way that leaves the extraction efficiency essentially unchanged. As one moves along a line of constant F_2 content and amplifier size, the pumping rate increases and the run time is reduced. The g_0L is increasing which would lower the extraction efficiency; however, the gain-to-loss ratio increases as we pump harder and this effect would increase the extraction and make up for the loss due to larger g_0L . In these figures, the aspect ratio has now been fixed at unity. The amplifier volume for each F_2 mixture has been replaced with the resulting aperture size in centimeters. One should note that the computed extraction efficiencies are slightly different from the 40 percent number assumed in order to draw the set of parallel lines of constant F_2 content and amplifier size. This is to be expected since the original 40 percent number was just a guess. The

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Figure 3.6-5. Scaling Maps for 1 and 2 atm Pressure amplifiers with Aspect Ratios of Unity.

correct procedure would now be to choose a second extraction efficiency and construct a second scaling map; this process would be continued in an iterative way until the map was self-consistent. We have chosen not to continue this process past the first map because the assumed and calculated extraction efficiencies are quite close and because it would not further illuminate the amplifier scaling technique. As a final input to those maps from the kinetics, we have taken the maximum value of g_0 for each run and converted it to a $g_0 L$ value and superimposed this information on the maps of Figures 3.6-5(a) and 3.6-5(b) in the form of dashed lines. As one pumps at a higher rate, the value of $g_0 L$ increases and at some point parasitic suppression becomes a problem; consequently, one of these lines will eventually be considered a bound on the operating space.

It should be noted that maps of this kind were also developed for the temperature dependent kinetics with the result that there was essentially no difference when the runs were terminated at the 50 percent F_2 "burn up" point and for pumping rates up to 1000 kw/cc. Under these conditions, the stored energy per unit volume was the same for both codes. There were small differences in the small signal gain (slightly higher values at low pumping rates for the kinetics with temperature dependence included) and slightly higher gain-to-loss ratios that will produce small changes in values of $g_0 L$ and the extraction efficiency on the scaling maps.

From these maps we can trade off run time versus pumping rate for various amplifier aperture sizes. Bounds are set by maximum values of $g_0 L$ and the damage threshold of 5 J/cm². However, we must also meet requirements associated with the e-beam constraints. These requirements or constraints can also be included in these design maps by drawing from the pulsed power scaling conditions described in Section 6.1. The pulsed power results show that there is a minimum run time associated with a PFN or PFL when operated at a given voltage and energy (one should refer to the pulsed power section to understand the assumptions used in generating these relationships). From the scaling maps, based on the kinetics, we have identified amplifier widths for 50 kJ modules where an aspect ratio of unity was assumed. For these amplifier widths, there is an e-gun voltage

that will give the most uniform deposition of the e-beam energy in the gas; and for this voltage and total energy, there is a minimum run time set for the PFN or PFL. These minimum run times and e-gun voltages can be cross plotted on the scaling maps to generate further operating bounds. Figures 3.6-6(a) and 3.6-6(b) show the result with the lower bounds set on the operating run times by the pulsed power. In addition, we have indicated the e-gun current density and impedance along these boundaries.

These scaling and operating maps derive their greatest utility from the fact that the extraction efficiency remains nearly constant over the entire map so that one can limit the various amplifier parameters with bounds set on the g_0L , the pulsed power run time, the damage limits, and then trade off pump rate, run time, aperture and optics size, and cost for a given energy module. To be sure the final scaling laws for the pulsed power and the final kinetics may change but we can still scale the amplifier using these techniques.

Figures 3.6-5(a), and (b) and 3.6-6(a), and (b) show that 1 atm. operation is very efficient, 45 percent extraction efficiencies due to a lack of collisional quenching, but the pulsed power impedance is extremely low, $\sim 1\Omega$, making this operating pressure an unlikely choice. At 2.0 atm the e-gun impedance is more reasonable but the extraction efficiency has dropped to 35-37 percent. It is likely that an optimum operating pressure for the 50 kJoule Module considered here will be 1.5 to 2.0 atm when trade-offs are made between extraction efficiency, e-gun impedance and run time.

One should be aware that these maps are made for a given energy, 50 kJ, and a particular amplifier aspect ratio for illustration. Other energies and aspect ratios can and should be considered. For example, if the aspect ratio were increased, then the extraction efficiency would go up if g_0L were held constant; if g_0L were held constant then the pumping rate would go down and the run time would increase for a constant volume case. All of these trade-offs would become apparent on a scaling plot like those already presented.

(a)

(b)

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Figure 3.6-6. Pulses Power Operating Bounds Cross Plotted on Scaling Maps for 1 and 2 atm Amplifier Modules.

3.7 Amplifier Layout

All amplifier designs considered in this study employ opposing e-beam geometries to insure uniform energy deposition and high KrF* production efficiency, η_{prod} . In a repetitively pulsed system, the laser gas would flow orthogonal to the e-beam and the optical axis of the amplifier. A gas curtain (soft wall), as described in Section 3.5 to help suppress parasitics, could be compatible with the geometries conceived. In fact, in a flowing gas system with soft walls, the amplifier could be overfilled by the extraction beams so as to reduce or eliminate any unextracted volume due to the angular multiplexing technique. In addition, all geometries considered would be compatible with solid windows, segmented windows or aerowindow options.

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193

Blank

IV. CRITICAL OPTICAL TECHNOLOGY

In this section areas of optical technology which will require development to fit system design specifications are presented. The section will be split into discussions on fabrication and on coatings of elements for the ultraviolet.

4.1 Fabrication

Fabrication of optical elements for a KrF scaling module becomes particularly difficult when the elements are large and/or aspheric elements. Fabrication of small elements is not a critical technological issue.

4.1.1 Mirrors

If large passive elements are to be produced there are very few vendors in the world for the glass blanks (e.g., Schott or Corning Glass). Similarly grinding and polishing can be accomplished at only a limited number of optical shops. For any particular surface the techniques for testing the element must be devised before the surface quality and figure can be predicted. For simple spherical optics the testing procedures are well understood. For an aspheric element which is intended for off-axis use the issues in metrology are typically the driving constraints on the cost of fabrication and on optical quality. In order to frame the problem in a general fashion the procurement of a mirror blank of meter dimensions and the fabrication of that blank into a polished asphere may take years and cost millions of dollars. A fast asphere of very high optical quality (for example, figure tolerances of $< \lambda/60$ in the visible) with low surface scatter and having dimensions exceeding 2 meters is similar to the NASA Space Telescope primary, which is a project in the hundred million dollar range. Large mirrors which have tight optical tolerances and are aspheric are a demanding technology.

In Section VII costs for spherical optical elements are presented. Spherical elements can be made with far less risk and cost than aspheric elements. From a reliability standpoint they can be more easily replaced than asphers. A spherical element does not have a unique optical axis; any radius acts as the optical axis. A large sphere may be synthesized of smaller spherical elements which are easily replaced and are interchangeable. If large elements can be spheres and can be segmented, then the optical system can be made much more reliable and risk-free. If one part of segmented mirror's coating is damaged the replacement of the segment can be done rapidly and inexpensively. An aspheric element cannot be segmented into interchangeable units. The critical issue in deciding for or against segmented mirror technology is how well can segmented spherical mirrors be mounted and aligned. It is unlikely segmentation would be advisable for spheres unless multimeter apertures were to become favored.

Small mirrors, in general, do not represent a critical technology in fabrication. In addition, there are many vendors that may be called upon to aid in a large production run for smaller optics (<60 cm).

4.1.2 Windows

More than for mirrors, the problems of damage and optical quality in windows are critical to making a high energy, laser fusion facility. Large transmissive optics may be used for the exit windows on the main amplifier. The critical issues for these applications are:

- Size
- Compatibility with gases in the amplifier
- Quality
- Segmentation
- Absorption
- Damage thresholds

Windows into the target chamber may also be required, and they, too, will have to be of high optical quality, low surface scatter and low absorption.

Because the beams have high flux levels the nonlinear index of refraction of a window material must be low if beam break-up

is to be avoided. The features desired for a high flux window material are availability in large sizes, low cost, low linear and nonlinear absorptions, low nonlinear index, ease in polishing, and chemical and mechanical resistance to degradation. The environmental compatibility of a window may be dictated by its coatings and not depend on the bulk material. On the basis of optical quality, absorption, damage, and availability in large sizes, fused silica is the leading window material candidate. The arguments in support of this are elaborated upon in Section 2.3. The critical window technologies that should be addressed for future phases of this program include developing the best possible window material and establishing how the largest required windows can be made. Segmented window technology should be addressed. The motivation to segmented windows, as pointed out in Section 2.3, is not only to reduce the largest single window which must be produced, but also to allow the window to be thin with the frame providing support. This will reduce the optical thickness in the glass and minimize the difficulties due to nonlinear effects.

4.2 Optical Coatings

Increased optical efficiency for transmissive and reflecting elements can be obtained by use of multilayer dielectric antireflection coatings and enhanced reflection metal/dielectric mirror coatings, respectively. Coating efficiencies and damage limits will have to be advanced to meet the design specifications imposed by the KrF laser fusion system.

The ultimate limit to performance of optical coatings is set by the absorption of the thin film materials themselves. The characteristics required of coatings are high optical performance, resistance to optical damage, and resistance to environmental hazards.

Optical performance refers to high reflectivity or transmission, according to the purpose of the coating. Damage resistance refers to attaining high damage thresholds over multipulse use on large aperture elements. In Section 2.1 we found the linear dimensions of the system scale inversely as the square root of the damage thresholds and the area

of the optical elements inversely as the damage thresholds. Increasing the damage limits of coatings is consequently an area in which advances will be immediately beneficial to this program. Resistance to environmental hazards includes a multitude of concerns such as making hard durable coatings that are not prior to degradation in normal atmospheric conditions or creating coatings with special immunity to chemical damage. For example, coated elements exposed to the interior of the KrF amplifier should be immune to fluorine attack. If a coated element is the final element facing the target, then the element must be kept sufficiently far from the target so as to prevent damage by radiation or debris. The stand-off distance will affect alignment tolerances throughout the system. It is possible that bare metal mirrors with high damage thresholds, immunity to environmental degradation and high reflectance may be developed for this purpose; however, bare metal surfaces used at normal incidence donot have high uv reflectivity. Possibly, a metal surface used at a grazing incidence could provide the desired performance.

At present, it appears that desirable design levels for damage thresholds for use in a KrF scaling module of the hundred kilojoule level are those given below.

<u>Pulse Length (ns)</u>	<u>5 to 20</u>	<u>200 to 700</u>
Desired Minimum	2 J/cm ²	3 J/cm ²
Currently Available 4.2-1	0.5 J/cm ²	2 J/cm ²

The desired minimum damage limits are chosen in accord with our baseline design, and the currently available limits are typical of those found in Reference 4.2-1. If coating technology advances beyond the desired levels suggested above, it would be beneficial technically and economically. These goals appear to be in general accord with ambitious, but realistic, goals of others working in the coating technology field. 4.2-1 The currently available coating damage limits are difficult to ascertain precisely because of the proprietary nature of the technology at individual vendors.

Beyond the technical issues involved in attaining coatings for a KrF scaling module requiring meter optics, there are constraints of scheduling and production. It is unlikely that there are any uncommitted facilities available in the world today for coating large substrates with multilayer dielectric coatings. Such a job would require the construction of coating chambers to be dedicated to this work. Construction will have to precede the coating of the element by at least a year to allow for characterization of a chamber.

IV. REFERENCES

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V. ELECTRON GUN TECHNOLOGY

5.1 Introduction

The e-beam/pulsed power portion of the KrF Scaling Module Study has considered a number of aspects of these major subsystems to evaluate their importance and impact upon a high energy power amplifier module (PAM) design. The analysis was performed to identify subsystem designs which use existing technology, limits to specific technology approaches, areas where profitable technology development could be pursued and to evaluate the system costs associated with a particular design approach.

The PAM and its major e-beam/pulsed power subsystems are illustrated schematically in Figure 5.1-1. The nominal PAM is basically cubic in proportion with the e-beam pump being injected into the gain medium from opposing faces of the PAM module. Each e-beam/pulsed power subsystem includes a diode, a pulsed power module and associated controls, and the guide magnetic field coils and their associated cryogenic and control system.

The issues which were addressed during the study include:

- E-beam pump requirements: it was necessary to relate the PAM pump requirements as developed from kinetics and optical extraction considerations back to the e-beam/pulsed power subsystem requirements. Scoping relations were developed, and detailed analysis performed, to relate the laser pump requirements to the e-beam performance. The e-beam voltage had to be consistent with the laser pressure to assure uniform pumping over the gain volume. The current density into the laser gas had to be consistent with the required kW/cm^3 pump rate.

Figure 5.1-1.

PAM System Schematic Illustrating the e-Beam/Pulsed Power Subsystems.

- Diode: the diode performance had to match the PAM e-beam requirements. Diode designs had to be evaluated for performance criteria which would satisfy the pump requirements. The important considerations were cathode type, emission uniformity, impedance collapse and high voltage integrity. Additionally, the foil support hibachi had to be compatible with the laser cavity pressure (and pressure pulse) and the obvious need for minimum beam interception.
- Magnetic guide field: For all PAM sizes and pump requirements studied, the e-beam self magnetic field produced significant pinching of the beam. To counter this beam pinch, it is necessary to provide an external guide field along the direction of current flow. Because the guide field would become a major cost, analysis of the minimum guide field necessary was important. Beam segmentation to reduce the self field, and thus the guide field requirement, was also analyzed.
- Pulsed power module: the pulsed power module driving the diode was evaluated to assure that the voltage, impedance and pulse length performance matched the laser driver conditions. Two possibilities exist for the pulsed power modules: lumped element pulse forming networks (PFN) or liquid dielectric pulse forming lines (PFL). Both approaches were evaluated for their suitability to the voltage impedance and pulse length performance of the diode.

The analysis of these e-beam/pulsed power subsystems was performed in close coordination with the kinetics and optical system designs.

5.2 Scoping Calculations

Some convenient relationships have been developed which allow scoping calculations of the e-beam/pulsed power requirements for the

nominal 50kJ PAMs of interest to this study. These scoping calculation relations were an early part of the analysis where they proved useful to quickly establish the e-beam/pulsed power operational requirements for a specific laser pump condition. These scoping calculations have been performed for 1 atm and 2 atm (argon) 50 kJ PAMs and are described in this section. Detailed analysis of specific PAM designs is performed in subsequent sections.

5.2.1 Beam Voltage

The dimensions of the PAM module varied depending upon kinetics considerations and the percent fluorine in the gas.

A convenient relation for estimating the beam voltage required for uniform e-beam energy deposition in a cube with two-sided pumping is: the range of the electrons should be $\sim 1.4 \rho W$ where ρ is the gas density in gm/cm^3 and W is the depth of the laser cell in cm. The beam voltage (to within ~ 20 percent) can then be obtained from the Berger & Seltzer tabulations (5.2-1).

Kinetics considerations of 1 atm and 2 atm PAMs show that dimensions in the range of 120 cm to 150 cm would be required. For argon at 1 atm and room temperature $\rho = 1.6 \times 10^{-3} \text{ gm/cm}^3$ and the corresponding beam voltages are:

<u>WIDTH-cm</u>	<u>1.4 ρW - gm/cm²</u>	<u>BEAM VOLTAGE - kV</u>
120	0.269	525
130	0.291	575
150	0.336	625

and for 2 atm and room temperature $\rho = 3.2 \times 10^{-3} \text{ gm/cm}^3$

<u>WIDTH-cm</u>	<u>1.4 ρW - gm/cm²</u>	<u>BEAM VOLTAGE - kV</u>
120	.538	925
130	.582	975
140	.672	1050

5.2.2 e-Beam/Diode Performance

The laser cell e-beam current can be determined from the power relation:

$$(\text{pump rate})(\text{volume}) = (\text{diode voltage})(\text{current})$$

or

$$I_L = \frac{(\text{PR})(\text{VOL})}{V}$$

where (PR) is the pump rate, V the gun voltage and I_L the total laser cell current. With pumping from both sides, the current per gun is

$$I = \frac{(\text{PR})(\text{VOL})}{2V}$$

To obtain the current inside the diode, this current expression should be divided by the hibachi transmission efficiency, η_H .

$$I_D = \frac{(\text{PR})(\text{VOL})}{2V \eta_H}$$

The diode impedance is given by

$$Z = \frac{V}{I_D} = \frac{2V^2 \eta_H}{(\text{PR})(\text{VOL})}$$

5.2.3 Self Magnetic Field

The self magnetic field of the e-beam in the diode, in gauss, is given by $B_{\text{self}} = 0.2 I_D/R$, where I_D is the current inside the diode, in amps, and R is the beam radius, in cm. To calculate B_{self} we take $R = \frac{1}{2}D$ where D = the characteristic size of the laser module.

5.2.4 Pulsed Power Requirements

These scaling calculations have been used to characterize the e-beam/pulsed power requirements for 1 atm and 2 atm 50 kJ PAMs. The results are summarized in Tables 5.2-1 and 5.2-2 respectively. The tables have been developed for the parameters of 0.15%, 0.2% and 0.25% fluorine mixtures at pump rates of 200, 300, 400 and 500 kW/cm³. With each fluorine concentration there is a characteristic module dimension and a corresponding e-beam voltage. (Refer to Section 3.6 on amplifier scaling).

Table 5.2-1. Pulsed Power Operating Conditions for a 50 kJ, 1 atm, Amplifier.

Pump Time/Beam Current Per Gun/Diode Impedance/Beam Self Field

Beam Current Density/Adiabatic Temp. Rise/Power Absorbed in Foil

$\% F_2$ (PAM WIDTH/BEAM VOLTAGE) PUMP RATE -kW/cm ³	0.3% (150 cm/625 kV)	0.4% (130 cm/575 kV)	0.5% (120 cm/525 kV)
200	800 ns/771 kA/0.8 Ω /2 kG 24 A/cm ² /100°C/12 W/cm ²	1200 ns/540 kA/1.0 Ω /1.7 kG 22 A/cm ² /130°C/16 W/cm ²	1350 ns/470 kA/1.1 Ω /1.6 kG 23 A/cm ² /130°C/16 W/cm ²
300	650 ns/1160 kA/0.54 Ω /3.1 kG 36 A/cm ² /122°C/15 W/cm ²	850 ns/830 kA/0.7 Ω /2.6 kG 34 A/cm ² /140°C/17 W/cm ²	1000 ns/715 kA/0.7 Ω /2.4 kG 35 A/cm ² /150°C/18 W/cm ²
400	450 ns/1.6 MA/0.41 Ω /4.1 kG 49 A/cm ² /115°C/14 W/cm ²	600 ns/1070 kA/0.53 Ω /3.3 kG 44 A/cm ² /130°C/16 W/cm ²	725 ns/930 kA/0.56 Ω /2.9 kG 45 A/cm ² /140°C/17 W/cm ²
500	350 ns/2.0 MA/0.32 Ω /5.1 kG 62 A/cm ² /115°C/14 W/cm ²	450 ns/1357 kA/0.21 Ω /4.1 kG 56 A/cm ² /120°C/14 W/cm ²	550 ns/1180 kA/0.42 Ω /4 kG 57 A/cm ² /140°C/17 W/cm ²

Table 5.2-2. Pulsed Power Operating Conditions for a 50 kJ, 2 atm, Amplifier

Runtime/Beam Current Per Gun/Impedance. Beam Self Field/Beam Current Density.

Adiabatic Temp Rise/Power Absorbed in Foil.

$\% F_2$ (PAM WIDTH/BEAM VOLTAGE) PUMP RATE -KW/cm ³	0.15% (140 cm, 1050 kV)	0.2%(130 cm, 975 kV)	0.25% (120 cm, 925 kV)
200	1050 ns/370 kZ/2.8 Ω , 1.0 kG/13.3 A/cm ² 61°C/7.3 W/cm ²	1350 ns/320 kA/3.0 Ω , 1.0 kG/13.3 A/cm ² 73°C/8.8 W/cm ²	1650 ns/271 kA/3.4 Ω , 0.90 kG/13.0 A/cm ² 84°C/10.1 W/cm ²
300	700 ns/560 kA/1.9 Ω 1.6 kG/20 A/cm ² 61°C/7.3 W/cm ²	950 ns/485 kA/2.0 Ω , 1.4 kG/20 A/cm ² 78°C/9.3 W/cm ²	1150 ns/410 kA/2.2 Ω , 1.35 kG/20 A/cm ² 88°C/10.5 W/cm ²
400	550 ns/745 kA/1.4 Ω , 2.1 kG/27 A/cm ² 64°C/7.7 W/cm ²	700 ns/640 kA/1.5 Ω , 2.0 kG/27 A/cm ² 76°C/9.1 W/cm ²	850 ns/540 kA/1.7 Ω , 1.8 kG/27 A/cm ² 87°C/10.4 W/cm ²
500	430 ns/932 kA/1.1 Ω , 2.6 kG/33 A/cm ³ 63°C/7.5 W/cm ²	550 ns/800 kA/1.2 Ω , 2.4 kG/33 A/cm ² 74°C/8.9 W/cm ²	700 ns/680 kA/1.3 Ω , 2.3 kG/33 A/cm ² 89°C/10.7 W/cm ²

The calculated parameters, based upon specific pump times, are:

- Diode current
- Diode impedance
- Self magnetic field
- Diode current density
- Foil window adiabatic temperature rise
- Average power deposited in foil at 10 Hz

5.3 Cathode Design

The cathode design is of obvious importance to the successful operation of the PAM. A number of cathode materials/designs are possible including both hot and cold cathodes. A cursory evaluation of hot cathodes indicated a number of disadvantages, principally significant thermal and vacuum penalties, so that only cold cathodes were considered suitable. At the current densities required for the shortest pulse mode, it would be possible to operate dispenser cathodes, but at a severe thermal penalty. Operating temperatures would be in the 1200°C to 1300°C range and the radiated heat would amount to $\sim 30 \text{ W/cm}^2$ or $\sim 50 \text{ kW}$ for a 125 cm x 125 cm cathode.

Tantalum blade cathodes have been characterized by an imaging of the blades on the anode when a strong guide field is used. The current density is highly nonuniform and unacceptable for laser pumping. Recent developments on carbon felt emitters have demonstrated that cold cathodes can operate at the level of performance required for pumping a large volume PAM. This includes uniform emission in the presence of a strong external guide magnetic field. X-ray pinhole photographs of the anode plane current density have demonstrated that carbon felt cathodes emit very uniformly in the presence of strong (4 kG) guide fields.

The other important characteristic of the carbon felt as a cathode is that there is little impedance change during the pulse, or the pulse lengths under consideration. This is illustrated by the current and voltage waveforms in Figure 5.3-1. This data was taken on the DARPA

Figure 5.3-1. Typical Voltage and Current Waveforms from the DARPA Rep-Rate Facility Using a Carbon Felt Cathode.

repetitively pulsed e-gun facility at MLI. The Child's Law corrected impedance demonstrates that there is virtually no impedance change for pulses $\leq 1 \mu\text{s}$. As a result, carbon felt emitters appear to satisfy all of the PAM e-beam/pulsed power requirements.

5.4 E-Beam Energy Deposition Uniformity

Uniform deposition of the e-beam energy into the active gain volume of the PAM is of obvious importance to efficient operation of the laser system.

The three-dimensional Monte Carlo electron transport code BETA-II^{5.4-2} was used to calculate energy deposition profiles in the diluent gas of the various amplifier modules considered. A goal of ± 10 percent deposition uniformity throughout the amplifier volume was considered reasonable for the study. The important product of the BETA-II analysis was the e-beam voltage required to produce the desired deposition uniformity.

Factors affecting the uniformity of energy deposition in the active medium include e-beam energy, buffer gas composition, anode foil material and thickness, foil support structure ("HIBACHI") design, and strength of the magnetic guide field (employed primarily to prevent beam pinching in the diode) in the deposition region. The BETA-II code is able to take into account all of these effects. These considerations apply equally to a single shot and to a repetitively pulsed device. In a rep-rate device, however, hibachi design and foil choice become much more critical due to questions of thermal loading.

Preliminary studies of the deposition uniformity for chamber widths of from 1 m to 2 m suggested an approximate (± 20 percent) scaling law for the diode voltage required for two-sided pumping in the presence of strong (~ 10 -20 kG) guide fields: the electron range (as given by the continuous slowing down approximation, tabulated in Berger & Seltzer^{5.2-1}, and plotted in Figure 5.4-1) should be approximately

$$\begin{bmatrix} 1.2 \\ 1.4 \end{bmatrix} (\text{gas density}) (\text{chamber width}) \text{ for } \begin{bmatrix} \text{neon} \\ \text{argon} \end{bmatrix} \text{ buffer gas.}$$

Figure 5.4-1. Electron Range in Neon and Argon (from Berger & Seltzer).

The slightly larger coefficient required in the case of argon is needed to compensate for the increased loss due to scatter from argon ($Z=18$) compared to that from neon ($Z=10$).

This scaling law is useful to obtain a first order estimate of the diode voltage before making BETA-II runs. Also, as used in Section 5.2, the scoping calculations are useful for evaluating the entire e-beam/pulsed power subsystem requirements.

Detailed BETA-II calculations have been performed on the 50 kJ PAM with an active volume of $(125 \text{ cm})^3$ and an operating pressure of 2 atm. To account for the competing effects of electron scatter, which removes electrons from the gain volume, and the magnetic guide field, which constrains the electrons in the active region, the gain volume was zoned for the BETA-II runs as illustrated in Figure 5.4-2. A magnetic guide field, when present, was assumed to point in the injected electron (z) direction, perpendicular to the foils. Four mil titanium foils were assumed to be uniformly illuminated from the rear. Any electrons back-scattered from the foil to the vacuum diode region were automatically reflected back to the foil.

BETA-II runs were made to evaluate the voltage requirement for a 50 kJ, 2 atm PAM. The cavity dimensions were taken as 125 cm x 125 cm x 125 cm and the fill gas was assumed to be 2 atm of argon ($\rho = 3.3 \times 10^{-3} \text{ gm/cc}$). Three guide fields were considered

$$B_z = 0$$

$$B_z = 0.85 \text{ T}$$

$$B_z = 2.1 \text{ T}$$

The cases of $B_z = 0.85 \text{ T}$ and 2.1 T correspond to $5 \times B_{\text{SELF}}$ for pulse times of 500 ns and 200 ns, respectively. In all cases, the optimum voltage for uniform energy deposition was 1.2 MV.

The results of the energy deposition calculations are plotted in Figure 5.4-3 for $B_z = 0$ and 2.1 T and in Figure 5.4-4 for $B_z = 0.85 \text{ T}$.

Figure 5.4-2. Segmented Gain Volume for Evaluating Electron Loss.

Figure 5.4-3. Energy Deposition as a Function of Distance into the Laser Cell.

Figure 5.4-4. Energy Deposition as a Function of Distance into the Laser Cell.

In each case there are three curves for the three regions illustrated in Figures 5.4-2a and 5.4-2b. The probable error in this data is ± 15 percent.

As expected, the greatest loss occurs due to scatter in regions of type (1) (corners), the least from type (3) (interior cells). For a strong guide field, however, the differences are small, demonstrating the positive effect of a guide field on uniformity transverse to the e-beam axis.

The results of the energy deposition calculations are best summarized by accounting for the fractions of total electron energy lost to the foil window, deposited in the active gain volume, and scattered out of the active region to the chamber walls. These fractions expressed in percent are:

	Bz - T		
	0	0.85	2.1
Foil Loss	8	8	8
Scatter Out	45	2	1
Deposited in Ar	47	90	91

From these calculations it is clear that the guide field, which is essential for efficient e-beam transport within the diode, is also important for containing the e-beam within the active gain volume.

The 8 percent foil loss was calculated for a 4 mil titanium foil, which might be necessary for a repetitively pulsed electron gun. This foil loss would amount to only 4 percent in a single pulse device where the window could be 2 mil titanium foil.

The scattered energy loss is approximately proportional to B^{-1} . An electron will execute a "random walk" across the field with step sizes of the order of the gyroradius. It will diffuse a distance $x \sim \sqrt{Dt}$ in time t where the diffusion coefficient, D , is equal to the (step size)² $\times$ (collision frequency). The particle flux crossing a boundary will then be proportional to $\dot{x} \sim \sqrt{D/t}$. Since $D^{1/2} \propto$ gyroradius $\propto 1/B$, the scatter loss will be proportional to $1/B$.

This simple argument has neglected the fact that different electrons will carry differing amounts of energy to the walls, depending upon the electron's history. Also, it is questionable whether the step size, in a collision-dominated regime ($v_c \gg \Omega_e$) should be taken as the collisionless gyroradius. The above argument is meant only to be suggestive. More detailed analysis supported by experiments is required.

BETA-II runs are required for each design point considered since a number of factors impact the uniformity of energy deposition. Of particular importance is the Hibachi design. Detailed analysis is necessary to evaluate the blockage of the e-beam by the Hibachi rungs. Preliminary studies with BETA-II have shown that even for modest applied magnetic fields (~ 1 kG) and electron energies of 1.6 MV, "shadows" of a ~ 2 cm side Hibachi rung can extend ~ 10 cm into the gas before filling in by scatter. In the case of thicker rungs or very strong guide fields this shadowing effect may become quite significant, resulting in a spatial variation, or modulation, of the gain.

The choice of foil material and thickness will also influence electron energy deposition, though not dramatically. Titanium foils, likely to be employed in a single shot demonstration device because of their strength, were specified in the BETA-II runs illustrated in Figures 5.4-2 and 5.4-3. If aluminum foils were used to exploit their large thermal conductivity, energy loss and scatter (per unit thickness) by the foils would be reduced. Electron energy loss, eight percent in the cases studied above, scales linearly with foil thickness and with Z while rms scattering angle is proportional to $(\text{thickness})^{1/2}$ and to Z .

In the final design phase of the PAM, consideration should be given to the stopping power of the electric field that drives the return current of secondary electrons in the gas. Though two-sided (symmetric) injections will minimize this effect, earlier MLI studies have shown that moderate fields (~ 10 -20 percent) of the equivalent stopping field of the gas = e' (energy range) can be produced. The actual value will depend on the attachment rate of secondaries by fluorine.

5.5 e-Beam Window and Support Structure

The study addressed the important design issue of the e-beam window (the anode) and a high transparency support structure. For efficiency considerations the window has to be thin to minimize electron energy loss and yet sufficiently strong to withstand the pressure differential, including overpressure, between the diode vacuum and the laser cell (i.e., 2 atm static and 1 atm overpressure). Because window foils are not available in dimensions suitable for a 125 cm x 125 cm e-beam, the window hibachi was segmented using a window size consistent with readily available foil materials. Structural considerations also facilitate segmenting the windows.

A preliminary foil window design has been developed which meets the criteria of small foil size (i.e., window modularity), strength to withstand the 3 atm pressure differential, and high transparency. The design is illustrated in Figures 5.5-1, 2 and 3. The design uses hibachi modules (Figure 5.5-3) which fit into a support matrix, illustrated in Figure 5.5-1 which is the load bearing member.

The operational conditions considered were: 2 atm operating pressure plus 1 atm overpressure, or 44.1 psia, applied uniformly over the entire window and the e-beam pulses occurring with sufficient time delay to allow for subsequent conductive and convective cooling of the foil to ambient temperature. The design developed was for a single shot, finite life e-gun. Consequently, the dimensions and spacings of the hibachi ribs and the support structure were based upon stressing the foil, the hibachi, and the support structure to nearly the yield strength of the materials considered.

A number of materials were considered for the design. They were all characterized by yield strengths greater than 100,000 psi so that the transparency could be made as high as practical. The materials selected were considered to be typical of what is available and what is machinable,

Maximum pressure	3 atm (44.1 psia)
Foil material	.002 in. thick titanium foil
Backup structure	17-4 pH stainless steel
Transparency	70% for single shots (limited life)
Maximum deformation of back structure	1.27 @ 3 atm absolute
Maximum stress in ribs	123,400 lbs/in ² (90% of yield)
Maximum stress in foil	123,500 lb/in ² (90% of yield)

Figure 5.5-1. Foil/Hibachi Design for a 50 kJ, 2 atm PAM.

Figure 5.5-2. Cross Section Illustrating the Foil Window, the Hibachi and the Hibachi Support.

Figure 5.5-3. Details of the Hibachi Module.

although further optimization could result in the selection of other materials. The materials considered in this design were .002 inch thick titanium 6AL - 4V foil for the e-beam window. The 6AL - 4V has a yield strength of about 136,000 lbs/in². For the hibachi and support structure, a precipitation hardened stainless steel, 17-4PH, was selected. It has been assumed that this material can be heat treated to a yield strength of 170,000 lbs/in².

The dimensions of the Hibachi and the support structure are illustrated in Figures 5.5-1, 2 and 3. All dimensions were chosen to operate at ~90 percent of yield. The configuration of the support structure was selected to be a square pattern of vertical and horizontal ribs spaced about seven inches apart. This was judged to be a reasonably dimensioned backup structure configuration based on consideration of manufacturability and material availability.

5.5.1 Stress Analysis: Foil and Hibachi

The stresses in a clamped rectangular foil acting as a uniformly loaded membrane can be approximated with the expression

$$\sigma = n \left[p^2 E \left(\frac{a}{t} \right)^2 \right]^{1/3}$$

where:

- p = applied pressure (psi)
- E = Youngs modules of foil (lbs/in²)
- a = length of slot (in)
- t = foil thickness (in)
- n = coefficient (no dimensions)

The coefficient, n, varies with location of the high stress points in the foil, i.e., center of short sides or center of long sides, and also the ratio a/b where b is the width of the slot.

This equation is reasonably accurate as long as the edges are considered fixed and rigid. The actual stresses occurring in the hibachi foil may be somewhat greater due to the deformation of the support structure, but this has not been taken into account. The coefficient n , which denotes the highest stress occurring in the center of the long side of the slot, is .20 for a length-to-width ratio of $a/b = 5.0$. For the Hibachi foil/slot dimensions considered, as shown in Figure 5.5-3, the maximum stress is then 123,500 lbs/m².

At a rib spacing of 1.068 inches and an effective rib length of 5.34 inches the load acting on each rib is:

$$W = 251.5 \text{ lbs}$$

The maximum bending stress is then:

$$S_{B \text{ max}} = \frac{M_{B \text{ max}}}{A} = 153,000 \text{ lbs/in}^2$$

vs. the 170,000 lbs/in² yield strength readily attainable with heat treated stainless steel 17-4PH.

5.5.2 Stress Analysis: Support Structure

A square patterned, pressure loaded rib structure consisting of equally spaced rectangular beams has been analyzed assuming:

- Square configuration
- Outer support is rigid
- Stringers (beams) are equally spaced
- Spacing is same in either direction
- All beams are identically sized
- All beams pinned at rigid outer support.

The maximum bending stress in the center beams is then given by

$$S_{\max} = \frac{Pa^3}{16 N Z_{\text{beam}}}$$

And the maximum beam deflection is

$$Y_{\max} = \frac{5 Pa^5}{(768) N E I}$$

where: $a = 125$ cm (50 kJ), $N = 7$, $t = .75$ and $h = 1.75$ in. Assuming the material to be 17-4PH stainless steel having a Youngs modulus of $E = 28 \times 10^6$ lbs/in²:

$$Z_{(125)} = \frac{th^2}{6} = .38 \text{ in}^3 \text{ (for } a = 125 \text{ cm)}$$

$$I_{(125)} = \frac{th^3}{12} = .33 \text{ in}^4$$

$$S_{\max(125)} = 123,400 \text{ lbs/in}^2 \text{ (for } a = 125 \text{ cm)}$$

$$Y_{\max(125)} = 1.28 \text{ in (for } a = 125 \text{ cm)}$$

This design stress is adequate for a single shot e-gun and the resulting geometric transparency, 70 percent, is consistent with the design objectives of high efficiency.

Repetitive pulse operations would require a more conservative stress level in the foil window, the hibachi and the support structure.

Operation at 90 percent of yield could result in premature failure due to fatigue. The stress levels could be reduced by increasing the height of the hibachi and the support structure and by increasing the foil thickness.

A "deeper" hibachi and support structure would probably require a stronger guide field to reduce interception due to beam pinch. The effect of a thicker foil window could be offset by an increased cathode voltage.

Beam segmentation, discussed in Section 5.7, would have the effect of reducing the unsupported width of the window structure. This will have a significant impact upon the Hibachi/support structure stresses so that the geometric transparency can be increased if segmentation is utilized.

5.6 Magnetic Guide Field

The self magnetic field of the e-beam within the diode results in a $v \times B$ force which imparts a radially inward velocity to the beam. The result is pinching of the e-beam due to its self field.

Beam pinch is a problem for two reasons. If the pinching is severe, as is possible in the short pulse, high current diodes, the beam area can be reduced relative to the cathode area. The result is a non-uniform current density on the anode plane. Additionally, the beam pinch produces electron trajectories at the anode plane which have velocities nonnormal to the anode, resulting in increased beam interception by the window foil support hibachi.

The self field induced beam pinch can be counteracted by providing an external guide field along the e-beam direction. This is most easily done with a solenoid field produced with a Helmholtz coil pair. This coil geometry is illustrated in Figure 5.1-1. There is another benefit to the external magnetic guide field which is the confinement of the e-beam to

the laser gain volume. Electron loss from the laser gain volume due to gas scatter is reduced.

The effect of an external guide field upon the electron trajectories within the diode, and the corresponding hibachi transmission, was calculated for the purpose of establishing external guide field requirements. The analysis was performed by using the computer code, REFER, to calculate the electron trajectories in a space charge limited diode, taking account of both self and external magnetic fields. The hibachi beam interception was then taken to be the geometric fraction of the beam which had perpendicular velocities which would cause them to strike the hibachi support.

REFER, originally written by Sandia Laboratories, calculates the electron trajectories and impedance of an e-beam diode in a self-consistent way. The code, limited to cylindrical geometry, calculates: electron trajectories; impedance; current density at the anode as a function of position; and the three velocity components (v_z , v_R , and v_θ) as a function of radial position along the anode. REFER makes these calculations for a space charge limited diode taking account of an external guide field B_z .

The results of a typical set of REFER trajectory calculations are illustrated in Figure 5.6-1. This is a parametric calculation of electron trajectories, between the cathode and anode planes, for electrons emitted along a radial line. The trajectories are illustrated by a selected sequence of electrons equally spaced along the cathode surface. These calculations were performed for the diode conditions of 1.2 MV and an anode current density of 34 A/cm^2 . This level of performance corresponds to the diode conditions required for the 500 ns pump pulse. The trajectories plotted in Figure 5.6-1 are for a series of external guide magnetic fields varying from five times the beam self field to virtually no external guide field. Since REFER is limited to cylindrical geometry, it was not possible to duplicate the 125 cm x 125 cm square geometry of

Figure 5.6-1. Calculated Trajectories for a 50 kJ PAM, 500 ns Diode.

the 50 kJ module design. However, the square cathode was approximated by a circular cathode of equal area. The resulting radius was 70 cm.

Qualitatively, the effect of the guide field upon the electron trajectories can be seen from this series of trajectory calculations. For the condition of $B_z \ll B_s$ there is considerable beam pinch resulting in a beam size at the anode plane which is smaller than it was at the cathode. The result is a nonuniform current density distribution at the anode. As the guide field is increased, the diode electrons tend to follow the B_z lines with the result that there is less beam pinch. The higher external guide fields produce a uniform illumination of the anode and improve the hibachi transmission.

The principal problem created by the beam pinch is the increased hibachi interception. This reduces the system efficiency; it puts excessive thermal loading upon the hibachi and foil window, and because the radial velocity increases toward the beam edge, there will be a further nonuniformity of the e-beam current distribution. The interception by the hibachi was calculated based upon the hibachi geometry (see Figure 5.5-2) and the calculated velocity components at the anode plane.

The geometric transparency of the hibachi design discussed in Section 5.5 is 70 percent. This, of course, assumes parallel (two-dimensional) flow of the diode electrons. The increased hibachi interception, due to beam pinch, has been evaluated based upon the REFER calculation of velocity components as a function of radial position. The increase in the intercepted fraction of the beam is just that portion of the beam which, due to the v_r or v_θ velocity, drifts into the hibachi while travelling the depth of the hibachi structure.

The results of this analysis are plotted in Figures 5.6-2, 5.6-3 and 5.6-4 for a 50 kJ PAM driver operating with pulse times of 500 ns, 300 ns and 200 ns, respectively. These plots show the increased hibachi interception (beyond the 30 percent geometric) as a function of radial

Figure 5.6-2. Hibachi Interception Due to Beam Pinch.

Figure 5.6-3. Hibachi Interception Due to Beam Pinch.

Figure 5.6-4. Hibachi Interception Due to Beam Pinch.

position for various values of the external guide field in multiples of B_{SELF} .

From these plots it is apparent that without an external guide field the increased beam interception is unacceptably high (the beam intensity is reduced by 30-50 percent due to beam pinching). As the guide field is increased above the self field value, the beam interception by the hibachi is significantly reduced. Acceptable transmission efficiencies will be achieved at 3-5 times the self field; the exact value required will depend not only on the transmission efficiency, but also on the guide field cost versus efficiency trade-off, and the degree of nonuniform pumping allowed in the medium by optical quality constraints.

5.7 Segmented Cathodes

Because of the large amount of stored energy in the beam guide field, the cost impact on the total design is significant. For this reason, it may prove economic to segment the beam. The basic idea is to segment the one large beam into two or more magnetically isolated smaller beams which can be controlled with a smaller guide field. Since the energy is proportional to B^2 , and the guide field cost is approximately proportional to the field energy, a significant cost savings can be realized even if the main beam is only divided in two.

5.7.1 Segmentation Limits

The segmentation scheme is shown in Figure 5.7-1. The guide field in the laser gas volume would be reduced by a factor appropriate to the number of cathode subdivisions. Since the biggest part of the energy is in the fringing fields of the Helmholtz coil pair, there would be a major reduction in the total magnetic energy with segmentation. In order to fill the laser volume uniformly, the beam would be generated over a smaller cathode area and adiabatically expanded as shown. The beam would require

Figure 5.7-1. Segmented Diode with Beam Expansion.

a higher field at the cathode but since this would be over a smaller volume, it would not constitute a major amount of energy.

The limits on beam segmentation are determined by how close beams can be packed together. This comes down to practical issues of the size corona shields on the cathode surface and allowable electrical stresses. These constraints could be relaxed somewhat if the beams were to be drifted such that the center plane of the beam is also curved instead of straight. This requires a more complicated guide field but could be accomplished in principle.

As an example of beam segmentation, consider a beam with the following parameters:

V	=	1.2 MV
I _{GAS}	=	610 kA
I _{DIODE}	=	870 kA
J _{DIODE}	=	56 A/cm ²
Beam Width	=	1.25 x 1.25 m
T _P	=	300 ns

The average self field at the beam edge of a square beam is given by

$$B_S \approx \frac{\mu_0 J L}{4}$$

where J is the current density and L is the beam width. For the parameters above, $B_S \approx .21T$. At 1.2 MV and 125 cm beam width the number of segmentations is limited to two for reasons of the cathode structure-to-wall spacing and the allowable beam expansion. These considerations are discussed below, but if a one to two expansion is used, the beam at the cathode would have dimensions of 0.3 m x 1.25 m, and have a current density, J', of 112 A/cm². Since this beam would be nearly one dimensional, the field at the edge would

be given by

$$B_S = \frac{\mu_0 J^2 L}{8} = \frac{\mu_0 J L}{4} = 0.21 \text{ T}$$

The one to two expansion would then reduce the field in the gas volume to 0.11 T, a factor of two over the unsegmented beam self field. The field expansion would be accomplished with twin coils located in the drift region and in the cathode support structure. The added volume required for beam drifting would not increase the energy significantly.

The implementation of a segmented cathode design depends upon the electrical stresses. In short pulse technology (~ 50 ns), the surface ignition threshold occurs at ~ 300 kV/cm and at ~ 1 μ s ignition occurs at ~ 100 kV/cm. This is consistent with the $t^{1/3}$ dependence derived from whisker ignition considerations. At the 300 ns pulse length a 10 cm sideways spacing would give 120 kV/cm which should be safe. In the anode/cathode gap, 112 A/cm² requires a ~ 5 cm gap spacing and stresses of ~ 230 kV/cm. This points out the need for careful design of the edge of the cathode and corona shield so that ignition of the cathode support structure does not become serious. The one to two expansion allows for a 10 cm side gap and 5 cm corona shield around the cathode. Further segmentation of the cathode is probably impractical.

5.7.2 Beam Drift Consideration

One of the complications of the segmentation process is the need to drift and expand the e-beam. Electron beams have been transported in guide fields but they have not been subjected to the stringent requirement of low transverse energy as exists here. An additional factor is the longer time scale (300-500 ns) compared to previous experience in the 50-100 ns pulse time.

The rate at which the beam can be expanded in an adiabatic manner is also important since it lengthens the system. The criteria for

adiabatic transport are somewhat dependent on the particular parameters, but an estimate is that the expansion be done in a time ≥ 10 gyro periods. The gyro period is

$$T_g = 2\pi \frac{\gamma m}{eB}$$

and the distance an electron travels in 10 gyro periods is

$$L \approx 20 \pi \gamma \beta \frac{mc}{eB}$$

For a 1.2 MV beam and field of $\approx .5$ T, $L \approx .7$ m. From this and considerations of the guide field shaping, transport distances ~ 1 m or so will have to be considered.

The other issue regarding beam transport is the question of field energy. When the field energy per unit length of the beam becomes comparable to the kinetic energy, the beam will not propagate well. This is because the magnetic field energy is supplied by the beam. Conventional practice has been to propagate the beam in a plasma channel where the beam magnetic field is cancelled by plasma currents and the net current ($I_{\text{BEAM}} + I_{\text{PLASMA}}$) determines the propagation. Usually $I_{\text{PLASMA}} \geq .9 I_{\text{BEAM}}$.

In propagating large beams, it is not certain that the plasma will be created as it is in short pulse high intensity ($10^3 - 10^4$ A/cm²) beams. Beam neutralization may have to be achieved by other means. One method of achieving the required neutralization is with a combination of background gas and further segmentation of the transported beam so that large self magnetic fields are avoided. The space charge in the beam can quickly be neutralized by a background gas. The neutralization time is given by

$$\tau_n = \frac{1}{N_0 v_e \sigma_I}$$

where N_0 is the neutral gas density, v_e is the beam velocity and σ_I is the ionization cross section ($\sim 10^{-18} \text{ cm}^2$). For a one torr background gas, $\tau_n \sim 1 \text{ ns}$.

If the gas does not break down to the extent required to form a plasma, the beam must be segmented to the extent that the magnetic energy is made much smaller than the beam kinetic energy. Physically what would happen if this were not done is that the beam, upon entering the drift region, would set up an opposing electric field which would perturb the beam. It has been demonstrated that the beam can be made to stop under conditions of large beam currents. We can estimate the opposing field by assuming the beam propagates and calculating the field created by the rising beam front. For simplicity, assume the beam is round. The field in the beam is

$$B_\phi = \frac{\mu_0 J D}{4}$$

where J is the current density and D is the diameter. The field generated by injecting a beam into a round tube can be calculated from Faraday's law

$$E\ell = \frac{d}{dt} \left(\frac{\mu_0 J D}{8} \right) \ell$$

where ℓ is the length of the beam.

$$E = \frac{\mu_0 D}{8} \frac{dJ}{dt}$$

For $D = 20 \text{ cm}$, $J = 50 \text{ A/cm}^2$ and a 50 ns risetime; $E \approx 3 \times 10^5 \text{ V/m}$. In a meter-long transport the loss would be serious. For only a 10 percent voltage drop, it may be necessary to handle the beam in 5 cm x 5 cm segments in the drift region.

This sample calculation indicates that the propagation of beams that are not magnetically neutralized needs to be evaluated in considerable detail.

SECTION V REFERENCES

- 5.2-1. M. J. Berger and S. M. Seltzer, "Studies in Penetration of Charged Particle in Matter", National Academy of Sciences Publication 1133, Washington (1964)
- 5.4-2. BETA-II was obtained by Maxwell Laboratories from the Radiation Shielding Information Center, Oak Ridge National Laboratories and modified for Maxwell requirements by its author, T. Jordan. Code documentation is available as ORNL Report 7 CCC117 wherein detailed comparison of code predictions with experiments designed to measure energy deposition and electron angular scatter are prescribed. Agreement of the code with the experimental data is extremely close.

VI. POWER CONDITIONING

6.1 PFN And PFL Scaling Relations

Two pulsed power technologies have been considered as drivers of the cold cathode diodes. For short pulse operation pulse forming lines, PFLs, (in which a slow Marx is used to charge an impedance and pulse length matched water coaxial pulse forming line) would be used while for long pulse operations, lumped element pulse forming networks would be used. The PFN design is simpler, and less costly, but there are pulse length limits below which it is impractical to go. Below this point the PFL would be more economic. The actual break point between the two design approaches requires a detailed analysis of specific point designs. However, practically, this transition is expected to occur in the 0.5 μ s region.

To assist in evaluating PAM designs, scaling relations of the PFL and PFN driven e-beam subsystems have been developed.

6.1.1 PFN Scaling

The PFN scaling laws have been derived from a Guillemin Type A voltage fed network as shown in Figure. 6.1-1. Here L_1 and C_1 are the series inductance and capacitance of the resonant circuit and L_2 and C_2 are parallel elements of an antiresonant circuit. In practice L_1 , C_1 , and L_2 , C_2 could represent several parallel circuits.

For a Type A circuit, the impedance and pulse length (FWHM of the power) are given by

$$Z = 1/35 \sqrt{L_1/C_1}$$
$$T_p = 2.25 \sqrt{L_1 C_1}$$

Figure 6.1-1. Two Mesh Guillemin Type A PFN.

The energy deposited in the gas is given by

$$W_D = 0.7 \frac{V}{Z} T_P$$

where V is the load voltage and the 0.7 multiplier is the approximate efficiency of transferring the diode energy into the laser gas. Substituting for Z gives

$$W_D = .23 \frac{V^2 T_P^2}{L_1}$$

Marx generator technology can be characterized by its dI/dt capability, or the normalized inductance typically expressed in $\mu H/MV$. This number is then a figure of merit for the particular design technology employed. Due to voltage division between the PFN and the load, and since L_1 is determined by the Marx voltage, it is best to convert to the Marx charge voltage V_C

$$\frac{V}{L_1} = \frac{1}{2} \frac{V_C}{L_1}$$

and for N_P paralleled Marx generators of inductance L_0

$$\frac{V}{L_1} = \frac{1}{2} N_P \frac{V_C}{L_0}$$

but

$$V_C = L_0 \dot{I}_0$$

so

$$\frac{V}{L_1} = \frac{1}{2} N_P \dot{I}_0$$

L_0 is the inductance of a single Marx building block and N_P is the number of parallel modules. The intrinsic rate of current rise $\dot{I}_0$ for low inductance Marx technology $\approx 1.2 \times 10^{12}$ A/s which corresponds to a figure of merit of $\sim .8 \mu H/MV$.

The energy delivered to the load can then be written as

$$W_D = 0.12 N_p \dot{i}_0 V T_p^2$$

and the energy delivered per volt is given by

$$\frac{W_D}{V} = 0.12 N_p \dot{i}_0 T_p^2 N_p$$

Using an $\dot{i}_0$ of 1.2×10^{12} A/s the maximum energy per Marx, which can be delivered at a voltage V and a pulse width T_p , is given by

$$\frac{W_D}{V} = 144 N_p T_p^2 \quad \frac{\text{kJ}}{\text{MV}}$$

where W_D , V and T_p are in kJ, MV and μs respectively. This relation is useful since it sets an upper bound to energy which can be delivered. It also sets a practical lower impedance limit per module of

$$Z = 7 V(\text{MV})/T_p(\mu\text{s})$$

The energy scaling has been plotted in Figure 6.1-2.

For a given e-beam dimension there is a limit to the number of Marx generators that can be used in parallel to achieve a sufficiently low inductance. To estimate this number, it has been assumed that Marx generators are arranged around the circumference of a circle encompassing a square beam of dimension D . The practical width of MLI Marx modules is ~ 0.6 m so that using this method of packing, the maximum number of parallel modules is

$$N_p \approx 7.4 D$$

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Figure 6.1-2. PFN Scaling Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length.

For electron beam dimensions less than ~ 0.5 m the maximum number is four since the corners of the modules will then be touching. The maximum number of paralleled modules is shown plotted in Figure 6.1-3 and should be taken as an approximate guide.

In principle, more modules could be stacked in a second layer and high voltage feeds added to connect them to the diode. This would have to be evaluated in the specific case to see if the low inductance could be preserved.

The 500 kJ, 1.2 MV, $0.5 \mu\text{s}$ beam requirement for a 50 kJ PAM could be met by this technology. For these values $W_D/N_P = 4.3$ kJ per Marx, giving a total of 12 Marx generators (six per side) which is allowable from Figure 6.1-3. However, if T_p is reduced to $.3 \mu\text{s}$ the required number of Marx generators is 32 (16 per side) which is impractical. This many Marx generators could not be located sufficiently close to the diode to preserve the low inductance in the case of the 50 kJ scaling module.

Scaling to short pulse lengths rapidly becomes impractical. Marx generators consist of series sections, with the number of series stages N_S being proportional to voltage. In this case the scaling relation has the form

$$W_D \propto N_P N_S i_o T_p^2$$

and total number of components is given by

$$N = N_P N_S \propto \frac{W_D}{i_o T_p^2}$$

Since the cost of a Marx scales in proportion to the number of series stages, the total system cost will scale proportional to the product of N_P times N_S , or T_p^{-2} .

Figure 6.1-3. Maximum Number of Parallel Modules vs Beam Width.

In addition, there are technical issues of packing the modules together. It is also seen from this relation that for a given voltage, energy and number of parallel modules, the pulse length scales as

$$T_p \propto \frac{1}{\sqrt{I_o}}$$

This means that major improvements in Marx technology must be made to go to significantly shorter pulses.

6.1.2 PFL Scaling (Single Line)

For the short pulse systems a Marx/PFL approach is required. To develop scaling laws for this approach it has been assumed that the system consists of a Marx generator charging a water insulated coaxial PFL and that the PFL is switched at peak voltage into a constant resistance load. The energy delivered is

$$W_D \approx 0.6 \frac{V^2 T_p}{Z}$$

where V is the load voltage, T_p the pulse length and Z the impedance. The factor of 0.6 includes 30 percent diode-to-gas loss and 10 percent PFL-beam coupling loss.

It can be shown that the electric field E_+ is related to Z , V and the pulse line diameter D by

$$E_+ Z = \frac{26.7 V}{D}$$

The positive stress E_+ is used because for line impedance in the 4-6 Ω range the positive breakdown dominates the design. The stress is given by

$$E_+ = \eta .23 T_e^{-1/3} A^{-0.58} \left(\frac{\text{MV}}{\text{cm}} \right)$$

This formula is the Lupton formation of the AWRE water breakdown data. Here T_e is the effective time (time from 63 percent of peak voltage to peak voltage) in μs , A is the outer line area in cm^2 and η is the derating factor. The derating factor to be used for the water line stress depends upon a number of considerations which include: the surface treatment of the coaxial lines, details at the conductor-insulator-water interface, water quality and others. A conservative derating would be $\eta = .8$ for a single shot and $\eta = 2/3$ for a repetitive pulse design.

The effective time is determined by the pulse length in that it affects the charging time. The minimum practical effective time is

$$T_e \approx 1.5 T_p$$

for switching at peak of a $1 - \cos\omega t$ type charging waveform. The area is implicitly a function of the pulse length since

$$A = \pi D u \frac{T_p}{2}$$

where u is the velocity of propagation in water. These factors all combine to give a stress,

$$E_+ \approx 9.4 \times 10^6 \eta D^{-.058} T_p^{-.39} \left(\frac{\text{V}}{\text{m}} \right)$$

where D is in m and T_p is in μs .

The equations for E_+ , Z and W_D can then be combined to give

$$W_D = 210 \eta D^{.94} T_p^{.61} V \quad (\text{kJ})$$

with D , T_p and V having units of m, μs and MV respectively. Another convenient way of writing this is

$$\frac{W_D}{V} = 210 \eta D^{.94} T_p^{.61} \quad \left(\frac{\text{kJ}}{\text{MV}} \right)$$

This is the energy delivered per side per unit voltage. This scaling relation is plotted in Figure 6.1-4 for values of D and η of interest.

The required diameter of the PFL is determined from the beam dimensions. For a square beam of dimension L on a side, the PFL diameter is

$$D \approx 1.8 L$$

This is the diameter required to enclose the diagonal and to allow for field shaping electrodes.

6.1.3 PFL Scaling (Multiple Lines)

For very low impedances, single pulse lines are volume inefficient and this limits the energy delivery at a given diameter. A more efficient design, and one which is potentially more economical, is a multiple line approach. For simplicity it is assumed that the lines are arrayed around a diameter consistent with the beam diagonal.

Figure 6.1-4. Plot of Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length. Single Line.

It is also assumed that the individual pulse lines have an impedance $>6 \Omega$ so the positive stress limits the performance.

The energy (per side) delivered by a multiple PFL system is given by

$$W_D \approx 0.6 \frac{V^2}{Z_1} N T_P$$

where Z_1 is the impedance of the lines and N is the number of lines. As before, the structure/coupling efficiency factor 0.6 is used. The number of pulse lines of diameter d that can be arrayed around a major diameter D is given by

$$N \approx \frac{\pi D}{d}$$

Assuming the positive stress dominates,

$$E_+ Z_1 = \frac{26.7 V}{d}$$

Substituting for d ,

$$E_+ Z_1 = \frac{26.7 V N}{\pi D}$$

Combining, the energy delivered becomes

$$W_D \approx 0.071 E_+ V T_P D$$

For this geometry the energy is independent of N as long as N is such that $Z > 6 \Omega$.

The stress E_+ is as given above with the exception that the area is the sum of the areas of the individual lines,

$$\begin{aligned} A &= N \pi d \frac{u T_P}{2} \\ &= \pi^2 D \frac{u T_P}{2} \end{aligned}$$

The area of this array is π times the area of the single line independent of N . This gives a value for E_+ ,

$$E_+ = 8.6 \times 10^6 \eta D^{-.058} T_p^{-.039} \left(\frac{V}{m} \right)$$

Substituting this in the energy relation above

$$W_D \approx 600 \eta D^{.094} T_p^{.061} V \quad (\text{kJ})$$

where D , T_p and V are in m , μs and MV respectively. This is seen to be approximately three times the energy capability of a single pulse line. The energy normalized to the voltage, W_D/V is shown plotted in Figure 6.1-5 for this case.

For a square beam of dimension L the array of multiple lines would be arranged on a diameter approximately equal to the diagonal of the square. This gives the relation between D and L , $D \approx 1.5L$. From this, the multiple line configuration can be chosen for a given V and T_p .

6.1.4 PFN/PFL Comparison

The two technologies can be compared in the 50 kJ PAM case. The electrical parameters are:

•Voltage	1.2 MV
•Energy Deposited	250 kJ per side
•Beam Dimension	1.2 m

For these beam dimensions, nine parallel PFN/s could be used and a 1.8 m multiple pulse line. The maximum energy capability is shown plotted in Figure 6.1-6 for these two cases.

It is seen that the crossover point for the two technologies is between $\sim .5 \mu s$ and $\sim .6 \mu s$. Either technology is allowed at the 250 kJ, $0.5 \mu s$ point but the PFN rapidly becomes inadequate at the shorter pulse lengths and the PFL becomes inadequate for longer pulses.

Figure 6.1-5. Plot of Maximum Energy Delivered per Side per Unit Voltage vs Pulse Length. Multiple Lines.

Figure 6.1-6. Comparison of PFN and PFL Technology at 50 KJ Design Point.

6.2. Magnetic Guide Field Technology*

The magnetic guide field requirements to assure efficient e-beam transport in the diode and the laser cavity are very significant in terms of field strengths and B-field energy. A cursory analysis of possible magnet designs shows that copper coils might reach the level of performance required, but it would only be for a single shot design with a very low shot frequency.

The only practicable source of the high field and large volume magnet systems required is superconducting designs. Current programs in support of the magnetic confinement fusion effort are building magnet systems of the sizes and field strengths of interest to this study. As a consequence, the guide field requirements are viewed as being within the state of the art.

Since the magnet design development had to proceed in parallel with other phases of the study, it was decided to develop a design which represented a practical upper bound of what would be required and what could be built. If a guide field less than the maximum design is required, the magnet design would be simplified and the cost would be less.

The maximum guide field requirement in the area of the diode was established at 3.5 tesla, corresponding to a 300nsec PFL and considering an applied field of 10 times the self field (see Section 5.6). This led to a peak field on the winding of approximately seven tesla. A field of this magnitude is well within the state of the art for a high current niobium titanium superconductor. Niobium titanium has been used at fields approaching ten tesla. Cooling of the superconductor would be performed by a liquid helium bath in the pool boiling mode. If the helium bath is at atmospheric pressure the operating temperature will be 4.2 K. At this operating temperature a great percentage of the magnet structure is required for the express purpose of providing

*The designs and analysis developed for this section were performed by Convair Division, General Dynamics, San Diego, CA.

thermal isolation for the helium vessel which contains the magnet windings. The conceptual design incorporates a liquid nitrogen cold wall which surrounds the 4.2 K cold mass. The entire vessel is then surrounded by a vacuum vessel to allow evacuation of the interstitial space around the cold mass and LN_2 shield. Finally, a liquid helium refrigerator/liquefier will be necessary to maintain the helium inventory against the unavoidable heat leaks associated with the leads, penetrations, supports, and thermal radiation.

The magnet itself will require an external electrical power and protection system for charging, discharging and protection. The mechanical design of the magnetic system has to provide access through the opposing Helmholtz coils for e-beam/pulse power access. Perpendicular to the B-field axis, unobstructed access has to be available for optical energy extraction and for gas flow in the repetitively pulsed case.

6.2.1 Design Description

The overall magnet system including a detailed cross section is shown in Figures 6.2-1, 2 and 3. The winding is wrapped on a 304L stainless steel bobbin which is completely lined with ground insulation. Each turn of conductor would be electrically separated from adjacent turns by fiberglass epoxy insulation. This insulation is machined to provide intermittent support between conductors. Helium can then flow freely throughout the winding by means of the interstitial spaces between insulators. The closeout to the bobbin/winding pack would be made with a 4.0 inch thick outer ring which is welded in place. Each bobbin sideplate is 3.0 inches thick to allow distribution of the 13,000,000 pound side load to the four intercoil supports.

Each of the intercoil supports would also be cooled to liquid helium temperature. This has two distinct advantages: 1) the material strengths are much higher at cryogenic temperatures and 2) the intercoil loads will not be transmitted through the cold mass supports. This

Figure 6.2-1. Side View of Superconducting Helmholtz Coil Pair.

Figure 6.2-2. End View of Guide Field Coils.

Figure 6.2-3. Cross Section of Superconducting Coil.

latter feature would markedly improve the thermal losses associated with the supports.

The entire cold mass structure should be encased in a liquid nitrogen cooled thermoradiation shield to reduce radiation losses. This shield can be fabricated by attaching small tubes to a copper or aluminum sheet. LN_2 can then be circulated through the cooling tubes from an external source. Liquid nitrogen thermal intercepts are conventionally used on the supports and penetrations to reduce helium refrigeration loads.

With the design shown, the cold mass supports need only support the gravity loads plus a margin for handling and seismic loads. Supports of this kind can be readily fabricated from a stack of epoxy fiberglass plates. The completed cold assembly would be encased in a 304 L vacuum vessel as shown in Figure 6.2-3. Multilayer thermal insulation will be installed on both sides of the LN_2 shield to further reduce heat leaks.

The following external equipment is typically required to maintain the magnet operation:

- Helium Liquefier/Refrigerator
- LN_2 Supply System
- Vacuum Module
- Power System
- Protection System
- Instrumentation and Control System.

6.2.2 Magnet Analysis

Calculations were made of the required amp-turns to produce 3.5 tesla in the diode region. The result was 12.56×10^6 amp-turns in each of the two solenoidal coils. Choosing a moderate current density of approximately 1750 A/cm^2 resulted in a coil pack $60 \times 120 \text{ cm}$.

Calculations were also made to choose an operating current. The current must be high enough to assure that the time constant during an emergency discharge is short enough to avoid conductor damage. An emergency discharge may be necessary in the event that sufficient cooling is not available locally to the conductor to retain the niobium titanium in the superconducting state. As the conductor temperature rises, the current will begin to flow in the copper stabilizer metal which surrounds the niobium titanium. When this occurs, joule heating of the copper causes the temperature to rise even further. If the magnet current is not reduced quickly, the conductor can be overstressed or even melt a quench.

General Dynamics magnet protection program QUENCH was used to calculate the peak adiabatic temperature rise in the normal zone during a magnet emergency discharge. QUENCH calculates the time and temperature evolution of system parameters by numerically integrating the basic equations governing the power balance and voltages during a quench condition. The actual integration is performed using temperature steps, and calculating the current from the temperature and the time from the current. The magnetic field is allowed to vary with the current.

The results indicate that a conductor current of 10000 A will result in a worst case temperature rise to 167 K if the discharge voltage is 800 V. This calculation provides a lower limit for the operating current consistent with insulation breakdown due to a high discharge voltage and reasonable temperature rise.

Detail analysis of the conductor cryostability can provide an upper limit to the operating current. Basically, as the conductor cross-sectional area is increased, i.e., with higher currents the available surface area to volume ratio decreases. This, in turn, reduced the cooling capacity of the helium bath. With the current density, current, and packing factor values chosen for this coil, it is safe to assume that a completely cryostable conductor can be designed. This means that the conductor surface will be sufficient to dissipate a larger amount of thermal energy than will be generated internally due to joule heating. Thus, if the conductor temperature is suddenly perturbed to 15 to 20 K by motion, the conductor will automatically return to the superconducting state at 4.2 K. There is always an energy input above which the conductor can not regain stability and this is why an emergency discharge circuit is necessary. Table 6.2-1 summarizes the magnet parameters calculated.

The magnetic forces on the conductor pack can be divided into two components, radial and axial. The radial loads which must be contained by the combination of the copper hoop tension and the 304 L outer ring have been calculated at 146,000 lbs/in of mean circumference. The out-of-plane load arising from the mutual attraction between the two coils is 20,000 lbs/in. This integrates to a total sideload of 13,000,000 lbs.

Table 6.2-1. Guide Field Magnet Parameters

SELF INDUCTANCE/COIL	10.16 H
TOTAL INDUCTANCE	22.8 H
STORED ENERGY/COIL	570 MJ
OPERATING CURRENT	10000 AMPS
OVERALL CURRENT DENSITY	1744 A/cm ²
CONDUCTOR CURRENT DENSITY	2604 A/cm ²
SUPERCONDUCTOR CURRENT DENSITY	50,000 A/cm ²
CONDUCTOR X-SECT AREA	3.84 cm ²
AMP-TURNS/COIL	12.56 x 10 ⁶
URNS/COIL	1256
CU: SC RATIO	18.2
RADIAL FORCE	147,000 LBS/IN.
AXIAL FORCE	20,000 LBS/IN.
DISCHARGE VOLTAGE (EMERG)	800 VOLTS
DISCHARGE CONSTANT (EMERG)	128 SEC.
DISCHARGE PEAK TEMP. (EMERG)	167 K
CHARGING TIME/10V SOURCE	171 MINUTES
CONDUCTOR WEIGHT	155000 LBS.

VII. BASE LINE DESIGN

7.1 Optical Train Staging for 50 kJ Modules

In this report technical trade-offs and restrictions have been developed as part of the conceptual design and have been integrated with an axisymmetric optical angular multiplexing concept. In the amplifier kinetics area a wide range of pumping rates, run times, gas mixtures and operating pressures have been considered; in the e-beam and pulsed power area trade-offs involving hibachi designs, operating pressure, deposition uniformity, guide fields, segmented cathodes and general PFN and PFL technology have been developed; and, as part of the overall optical design various beam packing arrays, alignment techniques, encoder development, the number of beam lines, and the optical quality have been considered. Before a point design can be accomplished and a large amplifier can be constructed, most or all of these trade-offs will have to be made. However, a general base line design and staging scenario can be developed independent of these choices. This is because the amplifier staging (from a technical point of view) will depend only on the power gains and aperture sizes achievable and not on the number of beam lines, amplifier run time, pumping rates, pulsed power technology, or cost. To be sure, the beam propagation paths required for beam separation will depend on the number of beams, and will, consequently, effect the overall system size, but will not significantly impact the amplifier staging, or even the alignment technique, because of the axisymmetric optical design.

7.1.1 Staging

As an example, a staging scenario, using a 50 kJ amplifier, is presented as a base line arrangement. Since the power gain is limited to about 15 (see section 3.4.1), the pre-amplifier must deliver ≈ 3.3 kJ to the final amplifier and its minimum aperture size is fixed, by the damage threshold of 5 J/cm^2 , at 25 to 30 cm.

Figure 7.1-1 shows a typical staging arrangement for a 50 kJ module; the 3.3 kJ pre-amplifier has a higher power gain than the final amplifier because of its larger aspect ratio and because its extraction efficiency need not be as high as that required for the final amplifier. In this example a 100 J amplifier following a 4 J angular encoder is used to drive the 3.3 kJ pre-amplifier - 50 kJ final amplifier. The master oscillator will deliver a few joules to the encoder. There are several ways in which this amplifier staging can be modified in order to drive several final amplifiers; in the figure we show the insertion of an additional pre-amplifier that drives up to four additional final amplifier-pre-amplifier pairs. An alternative scheme would be to design an angular encoder capable of generating 20 to 50 J so that the first pre-amp (100 J) size and energy output could be increased to drive several final amplifier-pre-amplifier pairs. The point is that there are many alternatives in the choice of staging arrangement and all of these are compatible with the axisymmetric angular multiplexing optical design developed in this report.

7.1.2 Damage Thresholds and System Size

The overall size of the optical system will depend on the total energy, the amplifier run time and the damage thresholds. Possible improvements in the damage thresholds will lead to considerable reduction in the system size, complexity and cost.

Windows: The minimum amplifier window size is essentially set by the long pulse (~ 500 nsec) damage threshold, $\epsilon_{th}(\text{long})$. The minimum window width, D , can be expressed as

$$D_{\text{window}} \geq \sqrt{\frac{E_{\text{PAM}}}{\epsilon_{th}(\text{long})}} \quad (7.1-1)$$

where E_{PAM} is the power amplifier total output energy. Large mirrors used in the amplifier will also have a minimum size given by Equation (7.1-1).

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Figure 7.1-1. Possible Staging Arrangement for a 50 kJ Final Amplifier System.

Mirrors: Mirrors used at separation planes will have their minimum size set by the short pulse (~ 10 nsec) damage threshold, ϵ_{th} (short).

$$D_{\text{separation}} \geq \sqrt{\frac{E_{\text{PAM}}}{N\epsilon_{th}(\text{short})}} \quad (7.1-2)$$

where N is the number of multiplexed beams. Here, the mirror size can be reduced by improving the short pulse damage threshold or increasing the number of beam lines. By increasing the number of beam lines, we add complexity; but the use of smaller optical elements may be less expensive to mount and fabricate. In any case, changing the number of beam lines does not alter the total optical surface area required to handle a given PAM energy. The only way to do this is to make improvements in the damage thresholds. As we can see from Equation (7.1-1) and (7.1-2), the total optical area scales as ϵ_{th}^{-1} so that a factor of 2 increase in ϵ_{th} significantly reduces the optical surface area and cost (see Section 7.2.2).

Overall Length: As described in Section II, the system length will depend on the propagation path needed for beam pre-separation between amplifiers and final beam separation prior to pulse compression. For a circular array of N multiplexed beams, the separation distance needed after the final amplifier is given by

$$l_{\text{sep}} = \frac{1}{2\pi\theta} \sqrt{\frac{2NE_{\text{PAM}}}{\epsilon_{th}(\text{short})}}$$

where θ is the half cone angle in radians for the circular array. Clearly, improvements in ϵ_{th} will not only reduce the optical component size and cost, but also the overall size of the optical staging train. Because the separation lengths in these multiplexing schemes are several hundred meters, one cannot overemphasize the importance of developing uv optical coating with higher damage thresholds.

7.2 Trade-offs and Cost Analysis

In this section a cost analysis is developed for the pulsed power options and the optical components. Using these scaling relationships, it is possible to evaluate the PFN or PFL pulsed power costs versus run time and compare this with the costs of the optical components. In this analysis a base line module of 50 kJ is used and the pulsed power costing is carried out for a 1 MJ system using 20 such modules.

7.2.1 E-beam Pulsed Power and Guide Field Costs

The cost of the e-beam/pulsed power subsystems will depend upon a number of parameters but principal among them are the voltage and pulse length assuming a constant energy size for the PAM module. For this study the analysis has concentrated on 50 kJ, optical, modules which are direct e-beam pumped from opposing faces. Assuming an optical extraction efficiency of 10 percent, the pump energy required is 500 kJ into the gas, or 250 kJ from each e-beam/pulsed power module. Taking this level of energy delivered as essentially constant, it is possible to develop some cost scaling relations for both PFN and PFL systems.

For both the PFN and PFL systems, the diode, guide magnetic field and control costs are virtually the same. The principal cost difference arises from the combined Marx-PFL cost versus the paralleled Marx of the PFN. Before cost scaling relationships are developed, it is necessary to determine or estimate the pulsed power operating efficiency, and this is done in the next section.

System Efficiencies

The performance of the e-beam/pulsed power subsystem in relation to the entire laser driver system has to be evaluated in terms of the entire system efficiency. Although the final e-beam/pulsed subsystem efficiency will depend upon the specifics of the system point design, some generic efficiencies can be developed to serve as guidelines in

developing the system scale-up plans. The important source of e-beam/pulsed power subsystem energy losses are tabulated for the PFN and PFL types of drivers. The efficiency breakdown is shown in Table 7.2-1.

Table 7.2-1
Efficiency Breakdown for Pulsed Power Options

Efficiency Item	PFN	PFL
Charging power supply	0.95	0.95
Marx charging loss	0.98	0.98
Marx to PFL charging	--	0.9
PFL losses	--	0.9
PFN losses	0.9	--
E-beam to anode plane	0.95	0.95
Hibachi (geometric)	0.8	0.8
Hibachi due to beam pinch	0.95	0.95
Foil losses (2 mil Ti equivalent)	0.96	0.96
Gas scatter out of gain volume	<u>0.96</u>	<u>0.96</u>
Combined Efficiency	55%	50%

These efficiencies are considered to be reasonable assessments of what would be realized in a fully optimized system, although development will be required in certain areas. For example, the 95 percent efficiency from the cathode to the anode plane implies an efficient transport/expansion system with slight losses to the expansion structure or at the beam edge. Also, although the hibachi point design of Section 5.5 had a geometric transparency of 70 percent, this can likely be increased in an optimized design to the 80 percent level.

As a consequence, the total system efficiencies for the PFL design is 50 percent and for the PFN design it is 55 percent from wall plug to laser gas. The efficiency for getting the energy from the diode into the laser gas, the product of the last five efficiencies, is 67 percent.

Cost Scaling Procedure

The cost of the pulsed power has been analyzed by determining the cost dependence of the major components as a function of the amplifier run time. The principal cost components considered include the diode, the guide magnetic field, and the PFN or PFL. The diode and guide field costs are the same for both PFNs and PFLs and are considered first.

Diode: Cost Scaling

At the 1.2 MV design point, the cost of the diode is essentially a function of the diode area. This arises primarily from the fabrication costs of the foil support and hibachi. Consequently, the diode cost, C_D , scales like

$$C_D \propto d^2, \quad (7.2-1)$$

where d is the diode dimension.

Guide Magnetic Field: Cost Scaling

The guide magnetic field necessary to assure a minimum loss to the hibachi structure has been taken as five times the self field. To reduce the energy, and cost, invested in the external guide field, it is further assumed that the diodes are segmented into two sections. This reduces the self field to approximately 65 percent of that of a square beam.

The guide magnetic field cost scales as the energy in the field. For either superconducting or copper magnets, the cost is typically 0.10 \$/J of field energy. The total guide magnetic field cost is given by the product of the field energy per PAM module, times the number of PAMs, times the field energy cost of 0.10 \$/J of field energy. The magnetic field volume is proportional to the laser volume, $V_B \propto d^3$, so that the field energy is proportional to $B^2 V_B$. The guide magnetic field cost is

$$C_B \propto B^2 V_B = B^2 d^3.$$

The self field is proportional to I/d , where I is the total diode current. The current is proportional to $1/\tau$, so that

$$B \propto 1/\tau d.$$

The resulting magnetic guide field cost is then given as

$$C_B \propto d/\tau^2. \quad (7.2-2)$$

These scaling relations for the diode and magnetic guide field costs are independent of the choice of a PFL or PFN and will be used for both pulsed power techniques.

Pulse Forming Lines: Cost Scaling

The major cost items of the PFL portion of the pulsed power subsystem are the Marx and the coaxial water pulse forming lines. For this system the Marx does not have to be particularly fast so that reliable, low cost designs, which are independent of the pulse length can be used. Thus, assuming a voltage twice the load voltage for the 50 kJ PAM, the Marx cost can be scaled in proportion to the energy required.

The scaling of the water line cost depends upon the pulse length since at longer pulse times the line impedance goes up and the electrical stress at which the line can be operated goes down.

With the efficiencies in Table 7.2-1, the energy delivered in N parallel lines of impedance Z_1 , is

$$W_D = 0.6 \frac{V^2}{Z_1} N \tau \text{ MJ}$$

where V is the load voltage in MV, and τ the pulse length in μsec . Also

$$Z_1 = Z N$$

where Z is the load impedance. From Section 6.1

$$E_+ Z_1 = \frac{26.7 V}{D}$$

Hence, the energy delivered can be written

$$W_D = 22.5 V D E_+ N \tau \quad \text{kJ}$$

Where D is the Diameter of the paralleled lines, in meters, and E_+ is given by the Lupton relation

$$E_+ = 23 \eta T_e^{-1/3} A^{-.058} \quad \frac{\text{MV}}{\text{m}}$$

Practically, the area of the lines is of the order of $2 \times 10^5 \text{ cm}^2$ so that the $A^{-.058}$ term is 0.5. Even if the area were half or double this amount $A^{-.058}$ is still ~ 0.5 . Thus

$$E_+ = 11.5 \eta T_e^{-1/3} \quad \frac{\text{MV}}{\text{m}}$$

Eliminating the electric field term from the energy relation yields

$$W_D = 258 \eta V D N \tau T_e^{-1/3} \quad \text{kJ}$$

The T_e is approximately one-third of the charge time $\pi\sqrt{LC}/2$ where L and C are the Marx inductance and capacitance respectively. For a 420 kJ Marx at 2.4 MV, $C = 150 \text{ nF}$. The inductance of the Marx can be taken as $2.4 \text{ MV} \times 4 \mu\text{H/MV}$ (twice the ANTARES figure of merit) so that $L = 9.6 \mu\text{sec}$ and

$$T_e = \frac{1}{3} \pi \frac{\sqrt{LC}}{2} \sim 1 \mu\text{sec.}$$

so that the scaling relation can be taken as

$$W_D = 258 \eta V D N \tau$$

or

$$D = \frac{W_D}{258 \eta V N \tau}$$

The costs of producing water pulse lines is largely dependent upon material costs and the fabricating costs of machining large diameter steel parts. From the above relation, it can be seen that the line diameter decreases with pulse time or the number of paralleled lines.

Experience has shown that pulse lines in the 3 to 4 foot diameter are the most cost effective to fabricate in terms of material and machining costs. Thus, the PFL costs scale as the number of paralleled modules and for a given load voltage and energy is proportional to $1/\tau$. There is a pulse length dependence to the pulse line cost but it is not very sensitive to the length of lines. The cost scaling for PFLs becomes

$$C_{PFL} \propto \frac{A}{\tau} + B\tau + C \quad (7.2-3)$$

where $\frac{A}{\tau} \gg B\tau$ in the pulse range of interest, and A, B, and C are constants to be determined.

Pulsed Forming Networks: Cost Scaling

The scaling of the PFN driver also depends upon the pulse time. For a fixed voltage, corresponding to the 50 kJ PAM module, the PFN cost is mainly a function of the number of paralleled modules necessary to realize a Marx inductance consistent with the resonant circuit requirements.

As seen in the pulsed power scaling relations developed in Section 6.1, the number of paralleled Marx modules is proportional to $1/\tau^2$. This functional dependence is only true when the resonant circuit inductance is less than the intrinsic Marx module inductance. At longer pulse lengths, only a single Marx module is required so that the cost, for a given voltage, is only proportional to the energy.

The PFN cost is independent of pulse time for pulses greater than $\sim 1 \mu\text{sec}$. Between $1 \mu\text{sec}$ and $0.5 \mu\text{sec}$ Marxes are paralleled in discrete steps so that the $1/\tau^2$ dependence is not completely accurate, while below $0.5 \mu\text{sec}$, the number of modules does increase rapidly at the $1/\tau^2$ rate.

To illustrate, the number of parallel modules given in Section 6.1 for low inductance Marx technology is

$$\frac{W_D}{V} = 144 N_P T_P^2$$

so that for a 1 μ sec pulse with $W_D = 250$ kJ and $V = 1.2$ MV

$$N_P = \frac{250}{1.2 \times 1.44 \times 0.1} = 1.4$$

As a consequence, two PFNs would have to be operated in parallel. For a 0.5 μ sec pulse, the number of paralleled modules is

$$N_P = 4 \times 1.4 = 5.6$$

so that six PFNs would be sufficient. A factor of 3 increase, not 4. For pulse times less than 0.5 μ sec, the interger round off becomes less significant so that the $1/\tau^2$ functional dependence is valid.

In developing a cost scaling for the PFN driver, the rating of the components (i.e., the energy per capacitor and the peak current and coulomb transfer per switch) decreases. So while the component count is increasing for shorter pulse lengths, the component cost also decreases.

The two paralleled PFNs required at the 1 μ sec pulse time could use Marx designs of the ATNARES type. For a 0.5 μ sec pulse, the Marx stage energy is down so that Maxwell plastic case capacitors could be used. The cost scaling for PFNs is taken as

$$C_{PFN} \propto A + B/\tau^2 \quad (7.2-4)$$

Where A and B are constants to be determined.

System Cost Scaling Relations

These functional dependencies can be combined into a single cost scaling relation for the PFN and PFL driven systems. The scope for the system evaluated was a 1 MJ (optical) system consisting of twenty 50 kJ PAMs. The subsystem count then breaks down to

- 20 50 kJ PAMs
- 40 Diodes (each segmented into two)
- 40 PFL or PFN drivers
- 20 Guide field coil pairs
- 1 Integrated control system
- 1 Integrated oil and water handling system

The e-beam/pulsed power module size for the 50 kJ PAM is ~250 kJ of delivered energy, or ~ 500 kJ of stored energy. Systems of this size are similar to other systems built by MLI so that this experience has been used to develop cost coefficients to convert the functional relations to cost algorithms.

To develop suitable cost algorithms, it has been assumed that the pulse lines have been fabricated out of stock in the 3 to 4 foot diameter range. Multiple lines of a smaller diameter are less expensive to fabricate than a single, large diameter line. The guide magnetic field requirement has been sized for a two segment diode with the guide field five times the self field.

The cost algorithms were developed on the basis of the dollars per joule of energy delivered to the laser gas for 50 kJ PAMs driven by 1.2 MV electrons. They are

$$C_{PFL}(\$/J_D) = [2 + 0.1 \tau + \frac{0.62}{\tau}] + 0.85 d^2 + \frac{0.1 d}{\tau^2} \quad (7.2-5)$$

$$C_{PFN}(\$/J_D) = [1.33 + \frac{0.67}{\tau}] + 0.85 d^2 + \frac{0.1 d}{\tau^2} \quad (7.2-6)$$

where τ is the pulse time in μsec and d the diode dimension in m. d is typically 1.25 m for the 50 kJ PAMs considered.

The nominal break point between the PFN and PFL designs is for pulse times of ~0.5 μsec . To illustrate, some typical pulse times are evaluated.

	PULSE TIME		
	0.3 μ sec	0.5 μ sec	1 μ sec
$C_{PFL} - \$/J_D$	6.8	5.1	4.2
$C_{PFN} - \$/J_D$	11.5	5.8	3.4

The cost scaling relations have been plotted in Figure 7.2-1 as a function of pulse time.

7.2.2 Optical Materials and Fabrication

The cost of optical components in a laser system is determined by the sum of the costs of the optical blanks, the fabrication of the blanks, the coating of the blanks and the mounting of the optics. In this section the costs associated with the blank materials, fabrication and coatings are discussed.

The energy output requirements of a laser fusion system are set by the target physics. The laser design will be structured to accomodate these needs. At each stage of the optical system, the energy and power requirements will be determined by this consideration. Because components damage at high energy and flux levels, the desired output energy sets lower bounds on the total area of optical surfaces needed for safe operation at each stage.

Cost trade-offs involve comparing different optical designs which meet minimum optical damage and quality standards. For example, it must be decided if it is preferable to use many small elements or fewer large elements. The relative cost of different optical elements is best understood on the basis of their cost per unit area. This data on materials and fabrication must be integrated with cost trade-offs on coating and mounting to give a representative cost model for the optical components.

Figure 7.2-1. Pulsed Power System Costs.

The optical components required in the KrF system will include transmitting windows, reflecting flats and reflecting mirrors. These can be considered in two size categories, (1) smaller elements needed in the front end of the optical system and in separated beam lines and (2) large elements required to span the full aperture of the largest beam size. Transmitting elements with apertures much in excess of a half meter appear impractical because of the difficulties in procurement of large and thick optical materials which are nonabsorbing and nonaberrating. If window technology permits, larger elements may be considered later. Larger material apertures can be constructed of mosaics of tractable elements. Reflecting elements, however, can be made to dimensions of several meters if it appears desirable.

Elements with Apertures Under 60 cm: Cost Scaling

The cost of small optical elements includes the sum of the costs of fabrication and the cost of the blanks. The cost of blanks varies with material, size, and with the process used to form the blanks. Cost figures here are given for Zerodur glass. The cost is roughly 35 to 40 dollars per pound. For a mirror or window of a given aspect ratio, the weight varies as the linear dimension cubed; consequently, the cost per unit area increases linearly with the aperture. This relation holds fairly well for mirror blanks.

For window blanks the requirements on the uniformity of the index of the material and the absence of inclusions, defects, and bubbles will be more severe than for mirrors. This is simply because the glass will be used in transmission. In addition, the blank cost will vary with the quantity of glass being handled in the melt. The glass may be continuously bottom tapped from a melt, poured off the lip of a melt or poured in single blank casts, and the process will influence the cost. Clearly, the cost will rise as rapidly, or more rapidly, than the weight when considering window blanks, and a brief survey of window material costs show their costs vary between the cost of mirror blanks and an order of magnitude larger. With this enormous uncertainty involved, an exponential divergence from the cost of mirror blanks was assumed.

We have found that up to about 25 cm in aperture, the costs of window blanks follow the \$40/pound law. An exponential with only small departure from linearity before 25 cm was therefore chosen, and quite arbitrarily the form chosen specifies an \$80/pound cost near 50 cm in aperture. This may be conservative.

The cost of fabricating the surfaces of the small elements should involve no special tooling. Moreover, single blanks can be safely transported by hand by one optician, and the optical operations are reasonably standard. We have found by pricing the fabrication of a few representative sizes of flat and spherical surfaces that the fabrication cost per unit area decreases with area for small optics (up to about 60 cm aperture). The cost data is shown in Figure 7.2-2. For simplicity, an exponential form was fit to the cost data points,

$$C = 1.4 + 3.6 \exp(-D/21.8) \quad (7.2-7)$$

for fabrication of flat surfaces and

$$C = 0.9 + 4.2 \exp(-D/23.1) \quad (7.2-8)$$

for fabrication of spherical surfaces, where C is the cost in \$/cm², and D the aperture in centimeters. The cost of the blank itself in \$/cm², B , is given by the relationship

$$B = 0.056 D \quad (7.2-9)$$

For mirror blanks, and

$$B = 0.056 D + 0.008 \exp(0.12 D) \quad (7.2-10)$$

for window blanks, again in units of \$/cm². The cost of fabricating window surfaces is roughly the cost of fabrication of two flat surfaces. Using Equations (7.2-7) to (7.2-10), expressions for the cost of fabrication and cost for blanks can be written for windows, flat mirrors, and spherical mirrors. These cost relationships are graphed in Figure 7.2-2. The cost of materials and fabrication (per unit area) for flats, spheres and windows all exhibit minima. The minima for reflecting spheres and

Figure 7.2-2. Cost per Square Inch as a Function of Clear Aperture.
For a Square Aperture Costs Must be Multiplied by $\pi/2$.

flats are rather gradual, and occur between 20 and 25 cm in aperture. Deviations from the aperture sizes will not drastically affect the system cost.

The cost per unit area for windows has a sharper minimum at approximately 35 cm in aperture. If the cost curve for the blanks is conservative, then this minimum is actually even sharper than shown and probably occurs at a smaller aperture. The transmitting windows are at a cost premium compared to the reflectors because they have twice as many optical surfaces and require superior materials.

Large Optical Elements: Cost Scaling

For optical elements in excess of about 60 cm in aperture, the weight of the blank makes it unsafe for one optician to hand carry. Special tooling becomes required for transport and for fabrication of the blanks. With this increased complexity and added difficulty comes an increase in cost. To the cost of fabrication and materials must be added the cost of special tooling. The cost per unit area for fabrication and tooling was determined by pricing a few representative elements, and was found to decrease with aperture over the 1 to 2 meter aperture range. The equation

$$C = 3.16 + 36.4 \exp(-D/33) \quad (7.2-11)$$

can be used to estimate the cost in \$/cm² for fabrication and tooling costs for a large spherical reflector of aperture D. Recall, this does not include coating and metrology costs. Metrology costs become important for any complex and difficult-to-test surface. The cost of the blank still roughly follows Equation (7.2-9) being directly related to the weight of the blank.

The cost per unit area for large reflecting spheres shows a minimum at approximately 1 m in aperture. However, at a 2 m aperture, the cost has only risen about 20 percent, which may not rule out the use of such large apertures if other system considerations offer offsetting advantages. The use of fewer large optical elements instead of a larger number of smaller ones generally reduces the complexity

of the optical system at the expense of introducing high risk pivotal elements.

It is always preferable to use elements smaller than 60 cm in aperture when possible because of the huge jump in cost at this size. A reflector of any diameter larger than 60 cm is over twice as expensive per unit area as elements in the smaller size range. This is a key cost consideration, and weighs heavily to minimize or eliminate the use of elements with apertures exceeding 60 cm. An understanding of the cost of the optical materials and fabrication for a megajoule device can be obtained by finding the cost per optical surface for the separated beams. The separated high flux beams occur in the final separator, delay lines, focusing optics and target chamber windows. Assuming small (60 cm) optics, the cost is

$$\frac{\$5}{\text{cm}^2} \cdot \frac{10^6 \text{ J}}{2 \text{ J/cm}^2} = \$2,500,000/\text{surface/MJ}$$

excluding the costs of coatings, mounting, and any special metrology.

7.2.3 Coatings

Optical coatings for 2500 Å are recognized as a critical technology for the KrF program. Current damage levels and efficiencies for anti-reflection and enhanced reflectivity coatings must be increased and long coating lifetime assured in order to keep the sizes of the optical elements tractable. High flux loadings are also needed for good energy extraction from the laser medium. The coatings needed are likely to be multi-layer dielectrics, and research in this area is needed. Even for smaller optics, there will not be a wide choice of coating laboratories. When the proper materials and techniques are found, coating the largest optics for KrF system will be a challenging job. They will require finely metered dielectric coatings over clear apertures of meter dimensions. These large optics may not carry a

central obscuration, and therefore will be different from most large telescopes in the techniques required for diagnostics and taking witness samples during coating. Currently, no vendor has experience in coating large optics for the uv with multilayer dielectrics.

The steps leading to a capability to produce efficient damage resistant (5 J/cm^2 pulses) coating are (1) researching thin films for the uv, (2) choosing a coating design, (3) researching the application of dielectric films onto large substrates so that they perform up to theoretical bounds, and (4) building the required facilities. Steps 1 and 3 are research and cannot be reliably scheduled and costed. Costs for basic research will not be included in the following cost estimates. However, chambers for coating large optics with multilayer dielectrics for the uv will probably need to be constructed, and must accomodate the largest elements used. Therefore, the additional single expense of the chamber must be included.

Cost Data

Coating costs can be divided among the following categories:

- Tooling Design (mountings and transport of optics)
- Procurement
- Tooling Fabrication
- Coatings Deposition

If a single optical type is to be produced in quantity, the tooling costs can be amortized over many runs, and for smaller optics, the tooling appears to be less consequential. The coating costs are generally constant for runs in a given chamber. Coating a 1 or 0.5 M optic in a chamber that will accept the 1 m optic costs about the same. At Perkin-Elmer, for example, we have coating chambers for applying multilayer dielectrics which can accept clear apertures of up to 40 cm and different chambers accept up to 1 m apertures. The cost of coating, per cm^2 , generally goes up with the size of the element, but decreases over the range in which one chamber will accept only one element. This relationship is illusory, and is a reflection only of the coating

chamber sizes available to a particular vendor rather than the difficulty of the job. Therefore, the data shown in Figure 7.2-3 must be considered with care. Three separate mirror sizes were priced; they were 0.5, 1.5, and 2.5 M in clear aperture. If the mirrors are square, this corresponds to elements which are about 0.3, 1.1 and 1.8 M on a side. The detailed data for this pricing is given in Table 7.2-2. We see the cost/cm² does not vary dramatically, and is between \$3 and \$4.5/cm². From the curves shown in Figure 7.2-3, we see that for 0.8 to 1 M optics, the cost actually drops below \$1.50/cm². If we wish to be somewhat speculative, we might infer that given a wider choice of chambers and facilities (recall in fact, there is not a vast choice), the curves might take on the shape shown in Figure 7.2-4. Finally, Figure 7.2-5 (a and b) shows the chamber costs. For costing a large fusion system it would be appropriate to include the cost of one chamber which can accommodate the size of the largest clear aperture required in the system. A few comments are in order about Figure 7.2-4. The curve for the smaller apertures was set at about \$1.50/cm². This is a rough average of the smaller aperture costs. It is assumed that the chamber used will be near, but not exactly optimized in size for the optics coated. The large aperture value was chosen to conform to the data given at 1.5 and 2.5 M. However, that data was given assuming diagnostics in a central obscuration or hole could be used, which may not be the case. Consequently, the cost is not chosen to tail off for very large apertures. The data is given assuming circular optics. The cost will be greater by a factor of about $\pi/2$ for square cross sections.

Figure 7.2-3. Coating Costs for Smaller Optics. Upper Curves Include the Nonrecurring Costs of Tooling; Lower Curves do Not Include the Nonrecurring Costs.

Table 7.2-2
Costs for Multilayer UV Optical Coatings

	0.5 Meter Dia		1.5 Meter Dia		2.5 Meter Dia	
	HOURS	\$	HOURS	\$	HOURS	\$
<u>Tooling Design</u>						
Designer	20	1,000	300	14,000	640	30,000
S. Eng. (Thermal, stress, mech.)	8	400	240	11,000	480	23,000
<u>Procurement</u>						
S. Eng.	-	-	40	2,000	120	6,000
<u>Tooling Fab</u>						
Mirror Space Frame Mount	-	1,000	-	6,500	-	18,000
Trunnion Mount	-	-	-	6,500	-	18,000
Mirror Transporter	-	-	-	4,500	-	12,000
Coating Distribution Tool	-	-	-	11,000	-	31,000
<u>Coating Deposition</u>						
S. Eng.	4	200	160	7,500	160	7,500
Methods	60	2,250	160	6,000	160	6,000
Technician	60	1,500	360	8,500	480	11,500
Total Coating Costs Per Mirrors	-	6,350	-	77,500	-	163,000
Area (Sq. Inches)	-	300	-	2,700	-	7,460
Cost/Sq. Inch	-	21.20	-	\$28.70	-	21.80

Figure 7.2-4. Cost per Square Inch as a Function of Clear Aperture: the Estimate is Based on the Data at 0.5, 1.5, and 2.5 m and the Cost of Using Chambers to Coat 1 and 0.4 Meter Optics.

Figure 7.2-5a. Equipment and Facility Costs for Large Optics Coating Chambers.

Cost in
Millions of
Dollars

Figure 7.2-5b. Coating Chamber Cost as a Function of the Clear Aperture of the Optical Element it can Accomodate.

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APPENDIX A
KINETIC RATE CONSTANTS

Table A-1
Electron Excitation, Ionization and Attachment Rates
(Based on a Maxwellian Distribution)

REACTION	RATE CONSTANTS		
	$K(\text{cm}^3/\text{sec-molecule}) = A T_e^\alpha e^{-B/T_e}$		
ARGON	$A(\times 10^{-9})$	α	β
$\text{Ar} + e \rightleftharpoons \text{Ar}^* + e$	5.0	0.74	11.58
$\text{Ar} + e \rightleftharpoons \text{Ar}^{**} + e$	14.0	0.71	13.1
$\text{Ar}^* + e \rightleftharpoons \text{Ar}^{**} + e$	890.0	0.51	1.52
$\text{Ar} + e \rightarrow \text{Ar}^+ + 2e$	23.0	0.68	15.76
$\text{Ar}^* + e \rightarrow \text{Ar}^+ + 2e$	68.0	0.67	4.18
$\text{Ar}^{**} + e \rightarrow \text{Ar}^+ + 2e$	180.0	0.61	2.66
$\text{Ar}_2^* + e \rightarrow \text{Ar}_2^+ + 2e$	90.0	0.7	3.5
KRYPTON			
$\text{Kr} + e \rightleftharpoons \text{Kr}^* + e$	9.0	0.72	9.96
$\text{Kr} + e \rightleftharpoons \text{Kr}^{**} + e$	23.0	0.72	11.47
$\text{Kr}^* + e \rightleftharpoons \text{Kr}^{**} + e$	900.0	0.5	1.51
$\text{Kr} + e \rightarrow \text{Kr}^+ + 2e$	27.0	0.7	14.0
$\text{Kr}^* + e \rightarrow \text{Kr}^+ + 2e$	70.0	0.68	4.04
$\text{Kr}^{**} + e \rightarrow \text{Kr}^+ + 2e$	198.0	0.62	2.53
$\text{Kr}_2^* + e \rightarrow \text{Kr}_2^+ + 2e$	90.0	0.7	3.53

Table A-1 (Continued)

REACTION		RATE CONSTANTS	
<u>FLUORINE</u>			
$F_2 + e$	$F^- + F$	<u>T_e(eV)</u>	<u>RATE $\times 10^{-9}$(cm³/sec-molecule)</u>
		0.5	6.5
		1.0	3.5
		2.0	1.6
		3.0	1.0
		4.0	0.67

Table A-2
Reaction Mechanisms and Radiative Properties for KrF

Reaction	Rate	Reference
<u>Neutral Reactions</u>		
$\text{Ar}^* + 2\text{Ar} \rightarrow \text{Ar}_2^* + \text{Ar}$	1.0×10^{-32}	1
$\text{Ar}^* + \text{Kr} + \text{Ar} \rightarrow \text{ArKr}^* + \text{Ar}$	1.0×10^{-32}	Estimate
$\text{Ar}^* + \text{Kr} \rightarrow \text{Ar} + \text{Kr}^*$	0.06×10^{-10}	2
$\text{Ar}^* + \text{F}_2 \rightarrow \text{ArF}^* + \text{F}$	4.7	3
$\quad \quad \rightarrow \text{Ar} + 2\text{F}$	3.1	3
$\text{Ar}^{**} + \text{Ar} \rightarrow \text{Ar}^* + \text{Ar}$	1.0	4
$\text{Ar}^{**} + \text{F}_2 \rightarrow \text{ArF}^* + \text{F}$	4.7	Analogous to Ar^*
$\quad \quad \rightarrow \text{Ar} + 2\text{F}$	3.1	Analogous to Ar^*
$\text{Ar}_2^* + \text{Kr} \rightarrow \text{Kr}^* + 2 \text{Ar}$	0.8	5
$\text{Ar}_2^* + \text{F}_2 \rightarrow \text{ArF}^* + \text{Ar} + \text{F}$	5.0	6
$\text{Ar}_2^* + \text{F} \rightarrow \text{ArF}^* + \text{Kr}$	3.0	Analogous to $\text{Ar}_2^* + \text{F}_2$
$\text{Kr}^* + 2\text{Ar} \rightarrow \text{ArKr}^* + \text{Ar}$	0.5×10^{-32}	Private communication
$\text{Kr}^* + \text{Kr} + \text{Ar} \rightarrow \text{Kr}_2^* + \text{Ar}$	2.7×10^{-32}	Private Communication
$\text{Kr}^* + \text{F}_2 \rightarrow \text{KrF}^* + \text{F}$	7.2×10^{-10}	3
$\text{Kr}^{**} + \text{Ar} \rightarrow \text{Kr}^* + \text{Ar}$	1.0	4
$\text{Kr}^{**} + \text{F}_2 \rightarrow \text{KrF}^* + \text{F}$	7.2	Analogous to Kr^*
$\text{Kr}_2^* + \text{F}_2 \rightarrow \text{KrF}^* + \text{F} + \text{Kr}$	4.0	6
$\text{Kr}_2^* + \text{F} \rightarrow \text{KrF}^* + \text{Kr}$	3.0	Analogous to $\text{Kr}_2^* + \text{F}_2$
$\text{ArKr}^* + \text{Kr} \rightarrow \text{Kr}_2^* + \text{Ar}$	1.0	Estimate
$\text{ArKr}^* + \text{F}_2 \rightarrow \text{KrF}^* + \text{F} + \text{Ar}$	6.0	Analogous to $\text{Ar}_2^* + \text{Kr}_2^*$
$\text{ArKr}^* + \text{F}_2 \rightarrow \text{ArKrF}^* + \text{F}$	3.0	
$\text{KrF}^* + 2 \text{Ar} \rightarrow$	$7.0 \times 10^{-32+}$	7
$\quad + \text{Kr} + \text{Ar} \rightarrow$	$65 \times 10^{-32+}$	7
$\quad + 2\text{Kr} \rightarrow$	$67 \times 10^{-32+}$	7
$\quad + \text{F}_2 \rightarrow$	$7.8 \times 10^{-10+}$	7
$\quad + \text{F} \rightarrow$	$7.8 \times 10^{-10+}$	Analogous to F_2

⁺ These quenching rates were experimentally evaluated from the product of $k \tau_{\text{rad}}$ and would be half these values assuming equal population of the KrF^* B and C states. τ_{rad} was determined as $\tau_{\text{rad}} = \tau(\text{B-state})$.

REACTION	RATE	REFERENCE
$\text{ArF}^* + \text{F}_2 \rightarrow \text{Ar} + \text{F} + \text{F}_2$	19×10^{-10}	7
$+ \text{Ar} \rightarrow 2\text{Ar} + \text{F}$	0.09×10^{-10}	7
$+ 2\text{A} \rightarrow \text{Ar}_2\text{F}^* + \text{Ar}$	40×10^{-32}	7
$+ \text{Kr} \rightarrow \text{KrF}^* + \text{Ar}$	16×10^{-10}	7

$\text{Ar}_2\text{F}^* + \text{Kr} \rightarrow \text{KrF}^* + 2 \text{Ar}$	1×10^{-10}	Estimate
$+ \text{F}_2 \rightarrow 2\text{Ar} + \text{F} + \text{F}_2$	20×10^{-10}	6
$+ \text{F}_2 \rightarrow 2\text{Kr} + \text{F} + \text{F}_2$	3×10^{-10}	6

Charge Transfer Reactions	Rate	Reference
$\text{Ar}^+ + \text{Kr} \rightarrow \text{Ar} + \text{Kr}^+$	0.3×10^{-10}	Estimate
$\text{Ar}_2^+ + \text{Kr} \rightarrow \text{ArKr}^+ + \text{Ar}$	7.5	8
$\text{ArKr}^+ + \text{Kr} \rightarrow \text{Kr}_2^+ + \text{Ar}$	1.0	Estimate

Dimer Ion Formation

$\text{Ar}^+ + 2\text{Ar} \rightarrow \text{Ar}_2^+ + \text{Ar}$	20×10^{-32}	9
$\text{Ar}^+ + \text{Kr} + \text{Ar} \rightarrow \text{ArKr} + \text{Ar}$	25	Analogous to $\text{Kr}^+ + \text{Kr} + \text{Ar}$
$\text{Kr}^+ + \text{Kr} + \text{Ar} \rightarrow \text{Kr}_2 + \text{Ar}$	25	10
$\text{Kr}^+ + \text{Ar} + \text{Ar} \rightarrow \text{ArKr}^+ + \text{Ar}$	10	Analogous to $\text{Kr}^+ + \text{Kr} + \text{Ar}$

Dissociative Recombination

$\text{Ar}_2^+ + e \rightarrow \text{Ar}^* + \text{Ar}$	0.6×10^{-6}	$[\text{T}_e(^{\circ}\text{K})/300]^{-0.66}$	11
$\text{Kr}_2^+ + e \rightarrow \text{Kr}^* + \text{Kr}$	1.2×10^{-6}	$[\text{T}_e(^{\circ}\text{K})/300]^{-0.55}$	11
$\text{ArKr}^+ + e \rightarrow \text{Kr}^* + \text{Ar}$	1.2×10^{-6}	$[\text{T}_e(^{\circ}\text{K})/300]^{-0.55}$	11

Ion-Ion Recombination

$\text{Ar}^+ + \text{F}^- \rightarrow \text{ArF}^*$	1.1×10^{-6}	7
$\text{Ar}_2^+ + \text{F}^- \rightarrow \text{ArF}^* + \text{Ar}$	1.1×10^{-6}	7

Reaction	Rate	Reference
<u>Ion-Ion Recombination</u>		
$\text{Kr}^+ + \text{F}^- \rightarrow \text{KrF}^*$	1.1×10^{-6}	7
$\text{Kr}_2^+ + \text{F}^- \rightarrow \text{KrF}^* + \text{Kr}$	1.1×10^{-6}	7
$\text{ArKr}^+ + \text{F}^- \rightarrow \text{KrF}^* + \text{Ar}$	1.1×10^{-6}	7
<u>Radiative Lifetimes</u>		
$\text{KrF}^*(\text{C}) \rightarrow \text{Kr} + \text{F} + h\nu$	$72 \times 10^{-9} \text{ sec}$	12, 13
$\text{KrF}^*(\text{B}) \rightarrow \text{Kr} + \text{F} + h\nu$	6.5	12, 13
$\text{ArF}^* \rightarrow \text{Ar} + \text{F} + h\nu$	4	14
$\text{Ar}_2^* \rightarrow 2 \text{ Ar} + h\nu$	16.7	15
$\text{Kr}_2^* \rightarrow 2 \text{ Kr} + h\nu$	12.5	Estimate
$\text{ArKr}^* \rightarrow \text{Ar} + \text{Kr} + h\nu$	12.5	Estimate
$\text{ArKrF}^* \rightarrow \text{Ar} + \text{Kr} + \text{F} + h\nu$	200	Estimate
$\text{Ar}_2\text{F}^* \rightarrow 2 \text{ Ar} + \text{F} + h\nu$	185	6
$\text{Kr}_2\text{F}^* \rightarrow 2 \text{ Kr} + \text{F} + h\nu$	180	16
<u>Laser Radiation Flux (ϕ) Cross Section</u>		
$\text{KrF}^*(\text{B}) + \phi \rightarrow \text{Kr} + \text{F} + \phi$	$2.6 \times 10^{-16} \text{ cm}^2$	12
$\text{F}_2 + \phi \rightarrow 2 \text{ F} + \phi$	1.5×10^{-20}	17
$\text{F}^- + \phi \rightarrow \text{F} + e + \phi$	5.6×10^{-18}	18
$\text{Kr}_2\text{F}^* + \phi \rightarrow 2 \text{ Kr} + \text{F} + \phi$	6×10^{-18}	Adjusted to fit absorption data
$\text{Ar}_2^+ + \phi \rightarrow \text{Ar}^+ + \text{Ar} + \phi$	1.3×10^{-17}	19
$\text{Kr}_2^+ + \phi \rightarrow \text{Kr}^+ + \text{Kr} + \phi$	1.6×10^{-18}	1
$\text{Ar}^{**} + \phi \rightarrow \text{Ar}^* + \phi$	6×10^{-18}	20
$\text{Kr}^{**} + \phi \rightarrow \text{Kr}^* + \phi$	6×10^{-18}	20
$\text{ArKr}^+ + \phi \rightarrow \text{Ar}^+ + \text{Kr} + \phi$	1.5×10^{-17}	21
<u>Superelastic Reactions</u>		
$\text{ArF}^* + e \rightarrow \text{Ar} + \text{F} + e$	1×10^{-7}	22
$\text{KrF}^* + e \rightarrow \text{Kr} + \text{F} + e$	2×10^{-7}	22
$\text{ArKr}^* + e \rightarrow \text{Ar} + \text{Kr} + e$	1×10^{-7}	Estimate
$\text{ArKrF}^* + e \rightarrow \text{Ar} + \text{Kr} + \text{F} + e$	1×10^{-7}	Estimate
$\text{Ar}_2^* + e \rightarrow 2 \text{ Ar} + e$	1×10^{-7}	Estimate
$\text{Kr}_2^* + e \rightarrow 2 \text{ Kr} + e$	1×10^{-7}	Estimate

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APPENDIX B

AMPLIFIED SPONTANEOUS EMISSION FLUX COMPUTATIONS

The evaluation of $\beta_{ASE}(xyz)$, $\beta_c(x,y,z)$ and $g(x,y,z)$ in the amplifier is a very lengthy and costly numerical calculation. Therefore, certain approximations have been used to minimize the computer time. First, the gain and flux distributions in the amplifier are only allowed to vary along the amplifier axis (the coherent beam propagation direction). Second, the ASE flux, $\beta_{ASE}(x,y,z)$, is calculated on the axis at each z position of interest and this value is assumed constant across the amplifier cross section, i.e., $\beta_{ASE}(x,y,z) \rightarrow \beta_{ASE}(0,0,z) \equiv \beta_{ASE}(z)$. Further, a cylindrical coordinate system has been used for simplicity and because part of the integration over the amplifier volume can be carried out analytically.

Figure B-1. Cylindrical Geometry Used to Compute the ASE Flux.

In this cylindrical coordinate system, the amplifier is divided into a number of slabs of thickness dz ; the gain, ASE flux and coherent flux are computed at the center of each of these cylindrical sections. Figure B-1 illustrates this geometry. The ASE flux, as specified in Equation (3.4 7), is rewritten using the substitutions

$$dv = 2\pi\rho d\rho dz$$

$$r^2 = \rho^2 + (z-z_0)^2$$

$$d\ell' = dz' \frac{\sqrt{\rho^2 + (z-z_0)^2}}{|z-z_0|}$$

$$g(x'y'z') \rightarrow g(z')$$

to obtain

$$B_{ASE}(z_0) = \frac{\tau_u}{\tau_R} \int_0^L \int_0^R \frac{g(z) \exp \left\{ \left| \int_{z_0}^z (g(z') - \alpha_{NS}) \frac{\sqrt{\rho^2 + (z-z_0)^2}}{|z-z_0|} dz' \right| \right\}}{2[\rho^2 + (z-z_0)^2]} \rho d\rho dz.$$

As one final simplification, we point out that the results can be written in a "dimensionless" form by making the following substitutions:

$$\rho \rightarrow \rho R,$$

$$z \rightarrow zL,$$

and

$$g \rightarrow g g_0$$

with these changes the flux and gain equations become

$$dB_c = g_0 L (g - 1/\gamma) dz, \quad (1)$$

and

$$g = \frac{1}{1 + \beta_c + \beta_{ASE}} \quad (2)$$

while the integral for $\beta(z_0)$ becomes

$$\beta_{ASE}(z_0) = \frac{\tau_u}{\tau_R} (g_0 L) \int_0^1 \int_0^1 (g(z)) \cdot \frac{\exp \left\{ g_0 L \left| \int_{z_0}^z (g(z') - 1/\gamma) dz' \right| \sqrt{\frac{\rho^2 + 4 \left(\frac{L}{D}\right)^2 (z-z_0)^2}{2 \left(\frac{L}{D}\right) |z-z_0|}} \right\}}{2 \left[\rho^2 + 4 \left(\frac{L}{D}\right)^2 (z-z_0)^2 \right]} \rho d\rho dz$$

where the parameters that define a particular calculation of $\beta_{ASE}(z_0)$ are now the aspect ratio L/D , the gain length product $g_0 L$, the gain-to-loss ratio $\gamma = g_0/\alpha_{NS}$ and the ratio τ_u/τ_R . The advantage of this form is that the amplifier dimensions do not enter the problem directly but rather through the quantities $g_0 L$ and (L/D) .

To evaluate this integral, we define

$$s(z, z_0) = g_0 L \left| \int_{z_0}^z [g(z') - 1/\gamma] dz' \right| / 2 \left(\frac{L}{D} \right) |z - z_0|$$

and
$$x^2 = \rho^2 + 4 \left(\frac{L}{D} \right)^2 (z - z_0)^2$$

to obtain

$$\beta_{ASE}(z_0) = \frac{\tau_u}{\tau_R} (g_0 L) \int_0^1 \frac{g(z)}{2} \int_{x_1}^{x_2} \frac{\exp[s(z, z_0)x]}{x} dx dz.$$

The integral over dx is found in the tables and leads to

$$\beta_{ASE}(z_0) = \frac{\tau_u}{\tau_R} (g_0 L) \int_0^1 \frac{g(z)}{2} \left\{ \ln x + \frac{s(z, z_0)x}{1 \cdot 1!} + \frac{[s(z, z_0)x]^2}{2 \cdot 2!} + \dots \right\}_{x_1}^{x_2} dz \quad (3)$$

where $x_1 = 2 \frac{L}{D} |(z - z_0)|,$

and

$$x_2 = 1 + 4 \left(\frac{L}{D} \right)^2 (z - z_0)^2$$

The integration over dz is carried out numerically by simply adding up the contributions from each slab of thickness dz .

Equations (1), (2), and (3) represent the final result for computing the effects of the ASE flux on the amplifier extraction efficiency. The computer technique used for computation begins by assuming a loaded gain distribution in the amplifier (usually $g_0/5$) the coherent and ASE flux is then calculated. These flux distributions are then used to generate a new gain distribution and the process is restarted with the calculation of new flux distributions. This procedure requires 10 to 15 iterations before a self-consistent set of flux and gain distribution is obtained. The amplifier length was divided into 40 to 100 longitudinal cells for these computations and, as formulated, lends itself easily to include multiple pass extraction by the coherent beam. The extraction efficiency for each case considered was computed from

$$\eta_{\text{ext}} = \frac{\beta(\text{out}) - \beta(\text{in})}{g_0 l}$$

In order to check this code, we have modelled amplifiers with large aspect ratios $L/D \gg 1$ to show that the extraction efficiency asymptotically approaches that for amplifiers without ASE. In addition, it is possible to compute exactly the ASE flux at the center of a sphere when the gain distribution is uniform over the spherical volume. The code calculated flux agrees with that analytical result. Further, the Mathematical Sciences Code agrees with the results of an extraction code written at Lawrence Livermore Laboratory and which has been reported at the 1980 Topical meeting on ICF by R. Haas.⁽¹⁾

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APPENDIX C

AMPLIFIED SPONTANEOUS EMISSION: SPECTRAL WIDTH CONSIDERATIONS

In Section 3.4 and Appendix B, the effects of Amplified Spontaneous Emission (ASE) on the amplifier extraction efficiency have been calculated and incorporated into the amplifier scaling analysis. In that work, the spectral width of the ASE flux was neglected as a simplifying approximation. In this Appendix, an estimate of the improvement in extraction efficiency, when the spectral width of the ASE flux is accounted for, is presented.

In Equation (3.4-1) the ASE term in the upper state rate equation was written as $gI_{ASE}/h\nu$; in order to include the spectral width of the ASE, this expression has been re-written as an integral

$$\text{ASE Term} = \left\langle \frac{g(\nu)I_{ASE}(\nu)}{h\nu} \right\rangle = \int \frac{g(\nu)}{h\nu} \left[\frac{dI_{ASE}(\nu)}{d\nu} \right] d\nu \quad (1)$$

over the product of the spectral extent of the gain and the number of ASE photons per frequency interval. The gain is written as $g(\nu) = g(\nu_0)G(\nu)$ where $g(\nu_0)$ is the line center loaded gain and $G(\nu)$ is the line shape factor and has the property that $G(\nu_0) = 1.0$. To obtain $[dI_{ASE}/d\nu]$ we return to Equations (3.4-4), (3.4-5) and (3.4-6) where the number of spontaneously emitted photons per volume element was written as N^*/τ_R . To include the spectral width this expression is replaced with

$$\left[\frac{dI_{spont}}{d\nu} \right] = K G(\nu) \frac{N^*}{\tau_R} \quad ,$$

where the normalization factor K must be defined as

$$K \int G(\nu) d\nu = 1 \quad .$$

Assuming these photons are emitted isotropically, the flux seen at (x_0, y_0, z_0) a distance

$$|r| = \sqrt{(x-x_0)^2 + (y-y_0)^2 + (z-z_0)^2}$$

from the emitting volume element is just

$$\left[\frac{dI_{\text{spont}}}{dv} \right] = \frac{h\nu N^*(xyz) K G(\nu) dv}{\tau_R 4\pi |r|^2}$$

Since these photons experience gain as they propagate over the distance $|r|$, the flux due to spontaneous emission at (x_0, y_0, z_0) becomes

$$\left[\frac{dI_{\text{ASE}}(x_0, y_0, z_0)}{dv} \right] = \frac{h\nu}{\tau_R} \int \frac{K G(\nu) g(\nu z) \exp\left[\int A dz\right]}{\tau_R 4\pi |r|^2} dv \quad (2)$$

where $A = g(x', y', z') G(\nu) - \alpha_{NS}$,

and the line integral in the exponential is taken along the path from the emitting volume to the observation point.

This expression is identical (within a constant factor) to that for I_{ASE} used in Section 3.4 where the gain has been replaced by $g(xyz)G(\nu)$. It would be convenient if the differential ASE spectral flux could be expressed in terms of the "old" ASE flux used in Equation (3.4-6). This can be done by recognizing that the effect of $G(\nu)$ in the exponential factor is to remove ASE photons that are off line center and thus to narrow the spectral distribution of the resulting ASE flux. Therefore, Equation (2) can be written as

$$\left[\frac{dI_{ASE}(x_0 y_0 z_0)}{dv} \right] \cong \frac{h\nu}{\tau_R} K G'(v) \int \frac{g(xyz) \exp[-fAd\ell]}{4\pi|r|^2} dv$$

where $A = g(xyz) - \alpha_{NS}$ and $G'(v)$ is a narrower distribution than $G(v)$; i.e., $G'(v) \leq G(v)$.

Within this approximation the differential ASE spectrum becomes

$$\left[\frac{dI_{ASE}(x_0 y_0 z_0)}{dv} \right] = K G'(v) I_{ASE}^{(OLD)}(x_0 y_0 z_0)$$

where $I_{ASE}^{(OLD)}$ is the ASE flux used in our extraction efficiency calculations. The ASE term now becomes

$$(\text{ASE TERM}) \cong \frac{g(v_0) I_{ASE}^{(OLD)}}{h\nu} \int K[G(v) G'(v)] dv \quad (3)$$

so that the integral is identified as a correction term that is ≤ 1.0 . Using a Lorentzian line shape for $g(v)$ we have

$$G(v) = \frac{(\Delta\nu/2)^2}{(v-v_0)^2 + (\Delta\nu/2)^2}$$

and the constant K is found to be $K = 2/\pi(\Delta\nu)$. The narrowed distribution $G'(v)$ is defined as

$$G'(v) = \frac{[(\Delta\nu)f/2]^2}{(v-v_0)^2 + ((\Delta\nu)f/2)^2}$$

where f is the line width narrowing factor that is ≤ 1.0 . The integral in Equation (3) can be shown to be $f/(f+1)$ so that the ASE term is just

$$\text{ASE TERM} = \frac{g(v_0) I_{ASE}^{(OLD)}}{h\nu} \left(\frac{f}{f+1} \right) \quad (4)$$

Therefore, within the approximations of this calculation, we find that including the spectral width of the spontaneously emitted photons leads to a correction factor of $f/(f+1)$. Thus, in the ASE calculations presented in Section 3.4, we simply multiply the ASE flux (as calculated at line center) by the correction factor $f/(f+1)$. When $f = 1$, there is no spectral narrowing of the photon spectrum and the ASE flux is reduced by a factor of 0.5. When the spectral width is narrowed by a factor of two then $f = 0.5$ and the correction factor becomes 0.33.

To evaluate the effect of this reduction in ASE flux on the extraction efficiency one must use $I_{\text{ASE}}^{(\text{OLD})} f/(f+1)$ as the ASE flux in the gain loading equation

$$g = \frac{g_0}{\left[1 + \beta_{\text{coherent}} + \left(\frac{f}{f+1} \right) \beta_{\text{ASE}} \right]}$$

While actual computer calculations have not been carried out, it is expected that this change will not increase the extraction efficiency by more than 10 to 20 percent; this is because the nonsaturable loss sets a firm upper bound on the overall extraction of ~ 55 - 60 percent.

APPENDIX D

FLUID DYNAMIC ANALYSIS OF ABSORBING CURTAIN FLOWS

Since the absorption cross section of F_2 is very large at 250 nanometers, a curtain of gas that is a few centimeters thick and has a several percent F_2 fraction may be an important factor in preventing parasitic oscillations between the electron beam foils. If argon is used to provide the balance of this curtain gas, its density can be essentially the same as that of the laser gas in the cavity which has several tenths of a percent F_2 . Such a curtain of absorbing gas must be introduced adjacent to the foil surfaces and prevented from dispersing into the laser cavity. Fluid dynamic techniques for accomplishing this are depicted in Figure D-1 and described below.

Two classes of cavity flow have been considered: a uniform flow rate providing two changes of cavity gas between 10 Hz output pulses, and a stationary gas in the cavity (corresponding to a single pulse system). The foil aperture size is assumed to be 100 cm x 100 cm.

The flow velocity required to provide two changes of cavity gas between 10 Hz pulses will be 2000 cm/sec. The curtain gas can be introduced rather uniformly over the foil surface through a distribution network in the foil support structure. Since a shear layer will grow between the quiescent curtain gas and the moving cavity gas the foil must be recessed in the flow cavity sidewall far enough to keep the shear layer away from the foil surface for the 100 cm span. The height of this foil cavity is denoted by h_f in Figure (D-1). The shear layer will be turbulent in the cases of interest here.

The analysis of turbulent shear layer growth given by Abramovich⁽¹⁾ shows that the rate of growth of the mixing layer into the quiescent fluid is independent of the velocity difference and equal to

1) Cavity Flow, Quiescent Curtain

2) Cavity Flow, Curtain Flow

3) Quiescent Cavity, Curtain Flow

4) Quiescent Cavity, Segmented Curtain

Figure D-1. Foil Curtain Flow Configurations

$$Y_2 = 0.17X$$

where X is the length of the shear layer in the flow direction. To keep this layer off the foil surface after spanning the 100 cm foil aperture, the foil cavity height h_1 must be

$$h_1 = 17.0 \text{ cm}$$

The turbulent Schmidt number has been assumed equal to one in these calculations, so that the concentration profile is assumed to diffuse at the same rate as the velocity profile. Abramovich⁽¹⁾ shows that the shear layer width is related to the length in the flow direction

$$b = x/3.7$$

so that the flow wall must be raised by 20 cm at the far end of the foil cavity to intercept the total shear layer and avoid high concentrations of absorbing gas in the laser cavity. This downstream wall will actually be an open inlet to scoop off the entire shear layer at this station. This inlet will thus be 27.0 cm high measured from the foil surface.

According to the Abramovich analysis the uniform velocity induced in the "quiescent" entrained gas at infinity is

$$V_2 = 0.043 U_1$$

for the submerged jet. Using the gas density and foil area the required mass flow of entrained gas is calculated as 1,550 gm/sec. This is the required flow rate of curtain gas.

Both the depth of the foil cavity and the required curtain flow rate can both be reduced in the curtain flow is introduced at the same velocity and parallel to the main cavity flow, as in configuration 2 in Figure D-1. The mixing layer then somewhat resembles the wake behind a flat plate moving in a 2000 cm/sec gas stream. The turbulence generated in the flow cavity wall boundary layer will control the mutual diffusivity of the two gas streams across their common boundary.

Schlichting⁽²⁾ has shown that the kinematic viscosity is constant in a 2-D turbulent wake and equal to

$$\epsilon_0 = 0.022 U_1 C_D d$$

The product of the drag coefficient and body diameter can be related to the thickness of the boundary layer momentum thickness on one side of the trailing edge of a flat plate through the expression for drag per unit width

$$D = \rho U_1^2 \delta_2 = C_D d \rho U_1^2 / 2$$

Assuming that the turbulent boundary develops from a distance L upstream of the foil cavity leading edge

$$\delta_2 = 0.036 L \left(\frac{U_1 L}{\nu} \right)^{-1/5}$$

where ν is the mixture kinematic viscosity taken as that of argon at STP, 229 micropoises.

Under the assumption of unity Schmidt number, the mass diffusivity is then:

$$D_T = \epsilon_0 = 0.0016 U_1 L \left(\frac{U_1 L}{\nu} \right)^{-1/5}$$

The steady, convective diffusion equation:

$$U_1 \frac{\partial n}{\partial x} = D_T \frac{\partial^2 n}{\partial y^2}$$

can be solved in terms of the complementary error function

$$\frac{n}{n_0} = \frac{1}{2} \operatorname{erfc} \left[\frac{y}{2 \left[\frac{D_T x}{U_1} \right]^{1/2}} \right] \quad (1)$$

solving for the stream lines where the relative concentration varies by 0.01 from the free stream values.

$$y = 3.24 \left[\frac{x D_T}{U_1} \right]^{1/2} \quad (2)$$

For the 100 cm x 100 cm foil aperture with 2000 cm/sec flow speeds the boundary layer thickness resulting from a 100 cm flow distance prior to the aperture leading edge is

$$\delta_2 = 0.208 \text{ cm}$$

giving a turbulent diffusivity of

$$D_T = 18.4 \text{ cm}^2/\text{sec}.$$

From Equation (2) the width of the shear layer growth into the curtain gas which is equal to the height of the upstream edge of the foil cavity is

$$h_1 = 3.11 \text{ cm}.$$

The mass flow rate of curtain gas at 4000 cm/sec through this area will be

$$\dot{m} = \rho u h_1 w = 1120 \text{ gm/sec}.$$

The full shear layer will be twice this width so the height of the exhaust scoop at the downstream edge of the foil cavity must be

$$h_2 = 6.22 \text{ cm}.$$

The second cavity configuration that will be considered has stationary laser gas in the cavity. The curtain gas then flows along the foil and is collected so that it cannot mix with the main body of laser gas and

increase the absorption in the laser cavity. The third configuration shown in Figure D-1 is the most straightforward of this type. Here the flow field resembles the upper half of a two-dimensional jet submerged in a quiescent fluid except for the boundary layer growing along the foil surface. The foil surface lies on the plane of symmetry of the jet and the initial region or jet core extends to the downstream edge of the foil aperture to assure curtain flow over the whole foil. Using the analysis of Abramovich⁽¹⁾ the length of the jet core is related to the jet half-height.

$$x_c = 9 h_1 \quad (3)$$

and the shear layer width at the end of the core region

$$h_2 = 2.40 h_1. \quad (4)$$

Applying these to the 100 cm x 100 cm foil aperture gives values for the inlet height at the leading edge of the foil cavity

$$h_1 = 11.1 \text{ cm}$$

and the scoop height at the downstream end of the foil cavity

$$h_2 = 26.7 \text{ cm.}$$

As in the case of the shear layer these values do not depend on the jet velocity. The required jet velocity can be determined from the requirement that the foil boundary layer be a small perturbation on the flow field. Under the assumption that only two percent of the jet momentum is lost to this foil boundary layer, the downstream boundary layer thickness can be related to the jet height

$$0.02 \rho u_2^2 h_1 = \rho u_2^2 \delta_2$$

Since the flow velocities will be low the foil boundary layer will be laminar (verified later in the analysis) for which

$$\delta_2 = 0.664 \left[\frac{v x_c}{u_2} \right]^{1/2}$$

and solving for the required velocity:

$$U_2 = 89300 \ v/x_c \quad (5)$$

substituting Equation (3).

The required mass flow then does not depend on the flow length.

$$\dot{m} = \rho u_2 h_1 w = 9920 \ \rho v w \quad (6)$$

Since the total jet momentum decreases linearly with jet height, which in turn decreases linearly with aperture width, and the momentum loss in the boundary layer decreases as the square root of aperture width divided by jet velocity, the jet velocity must vary inversely with aperture width to maintain a constant percentage momentum loss to the boundary layer. The jet mass flow which is proportional to the product of this velocity and the jet height is thus independent of aperture width. Narrower apertures with smaller jet heights require higher velocities to maintain the momentum balance.

Now the Reynolds number can be calculated

$$Re = \frac{u x_c}{v} = 8.9 \times 10^4$$

which is at least a factor of 4 below transition values. (2)

Applying this analysis to the 100 cm x 100 cm foil aperture gives the values:

$$x_c = 100 \text{ cm}$$

$$u_2 = 114 \text{ cm/sec}$$

$$\dot{m} = 227 \text{ gm/sec}$$

The foil flow may be divided into several 10 cm wide segments as shown in the fourth configuration of Figure D-1 in order to decrease the curtain thickness. Here the curtain flow is introduced at alternate foil support ribs and scavenged at intermediate foil support ribs. The foregoing analysis is applicable with

$$x_c = 10 \text{ cm.}$$

Using Equations (3) and (4) the inlet and exhaust heights become

$$h_1 = 1.1 \text{ cm}$$

$$h_2 = 2.67 \text{ cm}$$

Using Equations (5) and (6) the jet velocity and mass flow become

$$u_2 = 1140 \text{ cm/sec}$$

$$\dot{m} = 2270 \text{ gm/sec.}$$

It should be noted that this flow Mach number is 0.04 and that each of the 20 segments individually requires as much mass flow as the full window in the previous configuration for the reasons described after Equation (6).

The results of these calculations are gathered in Table D-1. with reference to Figure D-1.

TABLE D-1. ABSORBING CURTAIN FLOW PARAMETERS FOR A 100cm x 100cm E-BEAM FOIL APERTURE

Configuration	Upstream Foil Recesses h_1 (cm)	Downstream Scoop Height h_2 (cm)	Cavity Flow Rate U_1 (cm/sec)	Absorbing Gas Flow Rate U_2 (cm/sec)	Mass Flow (g/sec)
Cavity Flow, Quiescent Curtain	17	27	2000	0	1550
Cavity Flow, Curtain Flow	3.11	6.22	2000	2000	1120
Quiescent Cavity, Curtain Flow	11.1	26.7	0	114	227
Quiescent Cavity Flow, 10cm Segmented Curtain Flow	1.1	2.67	0	1140	2270