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EXPERIMENTAL PLAN FOR IFE TARGET INJECTION AND TRACKING DEMONSTRATION

by
PROJECT STAFF

and
University of California, Berkeley, California

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**GENERAL ATOMICS PROJECT 30007
DATE PUBLISHED: OCTOBER 1999**

CONTRIBUTORS

	<u>PHONE</u>	<u>E-MAIL</u>
<u>General Atomics</u>		
Dan Goodin	(858) 455-2977	Dan.Goodin@gat.com
Neil Alexander	(858) 455-2019	Neil.Alexander@gat.com
Gottfried Besenbruch	(858) 455-3077	Gottfried.Besenbruch@gat.com
Chuck Gibson	(858) 455-4071	Chuck.Gibson@gat.com
Sam Gurol	(858) 455-4113	Sam.Gurol@gat.com
Ken Schultz	(858) 455-4304	Ken.Schultz@gat.com
Long Nguyen	(858) 455-2892	Long.Nguyen@gat.com
Chuck Charman	(858) 455-4412	Charman@gat.com
Nathan Siegel	(858) 455-4057	npsiegel@hotmail.com
<u>University of California Berkeley</u>		
Ron Petzoldt	(925) 424-2550	petzoldt1@llnl.gov
Per Peterson	(510) 643-7749	peterson@nuc.berkeley.edu
<u>Los Alamos National Laboratory</u>		
Larry Foreman	—	—
Art Nobile	(505) 665-2283	anobile@lanl.gov
Jim Hoffer	(505) 667-4049	Hoffer@lanl.gov
<u>D3 Enterprises</u>		
Dennis Lieurance	(858) 277-8001	dlieurance@worldnet.att.net
David Madura	(858) 277-8001	Madura@poweron-inc.com

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Executive Summary

Activities and Accomplishments in Fiscal Year 1999

The Scope of Work and primary objectives of the Inertial Fusion Energy (IFE) Injection and Tracking tasks in FY99 were as follows:

Review previously proposed injection and tracking methodologies and evaluate additional technologies that have become available in the 5–10 years since the major IFE design studies were conducted. Coordinate with the IFE target and plant designers as necessary. Develop an experimental plan specifying material property measurements, measurements of the thermal environment effects on filled targets, and accuracy of injection demonstrations in the laboratory.

The FY99 accomplishments focused on several major areas (a) target and equipment evaluations necessary to define injection and tracking system requirements, (b) evaluations of potential injection and tracking system technologies that could meet these requirements, (c) selection of injection and tracking equipment for the experimental program (d) definition of Phase I* data needs and experiments to be conducted to address the key feasibility issues of injection and tracking, and (e) meeting with target and plant designers to integrate the efforts towards practical, optimized IFE targets. A detailed list of evaluations, calculations, and accomplishments is given in Section 6.

Background and Purpose of the Experimental Plan

The purpose of this document is to provide an experimental plan to address the issues associated with IFE target injection and tracking activities. Technical issues and the selection of experimental equipment are discussed, and a test program is recommended.

A commercial IFE power plant must place about 500,000 cryogenic targets each day (at a rate of 5–7 Hz) into a target chamber operating at 500°–1500°C. The only way to do this will be to inject the targets into the target chamber at high speed, track them and hit them on the fly with the driver beams. This must be done with high precision, high reliability of delivery, and without damaging the mechanically and thermally fragile targets. Key components of demonstrating a successful IFE target injection methodology are:

- Ability of targets to survive the chamber environment (target heating due to radiation and chamber gases)

*Phase I refers to the first portion (FY00 to FY03) of the Inertial Fusion Energy Development Strategy prepared under the auspices of the DOE Office of Fusion Energy (OFES) Virtual Laboratory for Technology (VLT); see Fig. 1–1.

- Accuracy and repeatability of target injection and tracking (ability to provide suitable beam steering and/or target steering, and shot-timing signals)
- Ability of targets to withstand acceleration into the chamber (strength of target components, including the DT itself).

Development of target injection and tracking is part of the IFE Development Strategy. During Phase I of this strategy, technology development activities will be carried out to investigate potential critical issues and to provide the basis for the decision at the end of Phase I on whether or not to proceed with construction of an IFE Integrated Research Experiment (IRE) in Phase II of the Development Strategy. The ultimate goal of the IFE target injection and tracking development program is to provide a successful demonstration of injecting prototypical IFE cryogenic targets into a surrogate chamber that is representative of an operating target chamber. This will be accomplished early in Phase II. For Phase I, the technical approach consists of (a) demonstrating processes and equipment at room temperature and (b) providing thermal and strength data to allow modeling of cryogenic performance. The objective of the Phase I work is to provide information for the decision to proceed with an IRE at the end of FY03.

Recommended Phase I Experimental Program

The proposed injection and tracking program for Phase I addresses issues associated with both direct drive and indirect drive target designs. Work will be performed both at General Atomics (GA) and at Los Alamos National Laboratory (LANL).

Summaries of the data to be gathered in Phase I are given below.

Target Thermal Response. This task affects primarily the direct drive target, as it lacks the surrounding hohlraum of the indirect drive target (which provides substantial protection from the imposed heat flux during injection). The heat load to the target during injection is composed of (a) thermal radiation and (b) heating by target chamber gases.*

The issue of thermal radiation exposure during injection is a matter of exposure time to the high temperature radiation. It is not necessary to have a target in motion to determine this effect. The technical approach chosen for Phase I demonstrations is to hold a target in a stationary configuration, filled and layered, and expose it to high-temperature thermal radiation. The effects on the target will be observed to determine its degradation with time. Measurement of DT degradation must be observed in-place, but other target materials (e.g., the capsule shell material) can be exposed to the environment for short times and then removed from the cryostat for post-test characterization. This

*For example, the reference SOMBRERO target chamber design for direct drive targets includes 0.5 Torr of xenon gas to control wall temperature excursions and ablation.

experimental work will be performed at LANL at existing ICF target facilities appropriate for work with DT.

Computational codes will be used to model and analyze the effect of target chamber gases on the injected target. The heat flow into the target will be calculated and aerodynamic considerations such as drag will be analyzed. Phase II includes verification of the Phase I (stationary) experiments and modeling work by injection of representative targets into a hot chamber. Experiments on heating of the direct drive target by chamber gases will also be conducted in Phase II, if necessary.

Target Injection Accuracy and Tracking. Issues of accuracy and repeatability will be addressed with experimental demonstrations. A new injection and tracking system will be constructed and utilized to demonstrate higher velocity direct drive and indirect drive injection and to measure the accuracy of placement and tracking. The construction of an experimental injector is a central part of the experimental plan described in this document. The injector will be utilized to measure accuracy with simulated targets during Phase I. It will be designed for later (Phase II) interfacing with cryogenic permeation fill and transport systems for a full, cryogenic injection demonstration into a high-temperature surrogate chamber.

Evaluations of the accuracy with which the driver beams must coincide with the target have resulted in a requirement of about 10 times more accuracy for direct drive target and beam pointing as for indirect drive targets.* It is anticipated that the accuracy requirement can be readily met for indirect drive targets using beam steering, but may require additional engineering controls for direct drive targets (e.g., temperature-controlled detector mounts, larger target chamber windows). A demonstration of tracking injected targets to the specified accuracy requirements and providing appropriate shot timing signals is an inherent part of this test/demonstration program. An evaluation and, potentially, a demonstration of target steering as an alternative to driver beam steering will take place. Driver beam steering is not part of this experimental scope.

Target Acceleration Response. It was assumed in the SOMBRERO IFE power plant design that the direct drive target could be injected at ~4 K and the DT fuel could heat up to 18.5 K (the shot temperature) during its transit across the chamber (at about 150 m/s). Recent work by LLNL indicates that a layered target can only sustain a temperature change of ~0.5 K in the DT without increasing the ice layer roughness enough to significantly degrade target performance. One parameter, which can potentially reduce target heating and respond to this narrowed requirement, is a higher injection velocity. For

*The requirements are to place a target to within 5 mm (for both indirect and direct drive), but to track the target and align the driver beams to within 20 μm for direct drive and to within 200 μm for indirect drive. This set of requirements is based on active driver beam steering; it may be more efficient to achieve the desired alignment for injection with target steering rather than driver beam pointing. Design studies to date have chosen beam steering so we have used this as our baseline.

a given injector length, higher injection velocities lead to higher acceleration forces on the target components. Therefore, limiting the temperature increase of the DT by utilizing higher injection velocities will result in higher acceleration on the target components. Tradeoffs involving the target temperature limits and corresponding ice layer roughness, chamber temperature and fill gas pressure, injector length, and potential target protective methods need to be further evaluated as target designs evolve. However, additional data are needed on the capabilities of the target components to withstand accelerations under the conditions of cryogenic injections.

The long-term technical approach chosen to address the target acceleration response is to construct and operate the experimental injector system to demonstrate the feasibility of target survival during the process. However, these demonstration results become available in Phase II of this program. Consequently, direct measurements of material property data, including DT strength, will be made at LANL during Phase I (see section below).

Target Property Measurements. The technical approach chosen to address the target acceleration response in Phase I is to measure target material properties under representative conditions and to analytically evaluate stresses imposed on target components. A key target component in this regard is the DT ice layer, and a key issue is its ability to withstand high accelerations. The currently available tensile strength data for hydrogen isotopes indicate acceleration forces near 1000 g's could be tolerated. However, these data are measured with deuterium at 16.4 K and no data are available for DT at 18.5 K. Indeed, this is very near the melting point for DT and the properties are changing rapidly in this temperature range. Also, the strength of the solid DT may be a function of time due to buildup of He-3 from tritium decay. Additional material property measurements will be carried out at LANL as needed and analytical evaluations will be used to address stresses imposed on components other than DT within indirect drive targets. This issue is much less critical for indirect drive targets, which have significantly lower injection velocities and accelerations.

Selection of an Experimental Injection and Tracking System

Requirements. Requirements for an experimental target injector system were derived based on first reviewing the expected injector system requirements for a power plant. While it is not absolutely necessary that the injector used for an experimental program have the same technical basis as one that would be applicable to a production unit, this is a highly desirable attribute. This approach has the advantage of using the experimental injector as a test bed to learn the intricacies of operating a given technology under representative conditions. Key experimental injector requirements are summarized in the following table.

Strategy and Testing Approach. The testing strategy is to inject representative targets into a high-temperature chamber (furnace) in order to simulate target thermal exposures representative of those in a power plant. An additional part of the testing strategy is to allow

testing of targets to destruction. This leads to a requirement for the experimental injector to reach accelerations of 5000 g's. Finally, one injector system for both direct and indirect drive targets is recommended to accomplish the testing goals of this program in an efficient manner.

Experimental Injector Requirements Summary

Requirement	Value
Target Speed (m/s)	400(Direct drive*) 180 (Indirect drive)
Acceleration (g)	5000†
Repetition Rate (Hz)	6 Multi-shot "burst" capability, upgradable to cryogenic
Target Mass (g)	0.005 (Direct drive) 1 (Indirect drive)
Target Diameter (mm)	5 (Direct drive) 12 (Indirect drive)
Target Temperature (K)	18–19 (Initial injections at room temperature)
Target Spin (rotations per second)	300
Target Exit Accuracy (m radians)	± 0.3 (Direct drive) ± 0.7 (Indirect drive)
Cryogenic Fuel Temperature Rise (from start to driver beam shot) (K)	0.5

*Capability of 400 m/s at 5000 g acceleration; implies a 1.6 m minimum experimental injector length.

†5000 g capability chosen to include capability of driving targets to destruction.

Injector System Evaluation

Light Gas Guns. A light gas gun is expected to meet all requirements and is the simplest and most straightforward technology for accomplishing target injection. Initial experimental work has already been conducted using a gas gun, and nearly every IFE power plant design study has recommended or assumed utilization of a gas gun for target injection. However, evaluations show that a simple gas gun is limited to about 400 m/s if operated at the low accelerations (1000 g's) expected to be necessary for cryogenic targets (much higher velocities are readily achieved with a gas gun but at higher accelerations). This may or may not be a significant limitation depending upon the design and injection velocity requirements of the final IFE target. More complex gas gun designs that include variable pressure control and mid-barrel gas injection may be capable of overcoming the

velocity limitation. The disadvantage of selecting a gas gun for this application is that the injection gas must be pumped away from the target chamber and may make target steering more difficult to accomplish.

Electrostatic Injector. Electrostatic accelerators could be utilized for the low mass of direct drive targets but are limited to accelerations of about 100 g's due to voltage breakdown considerations (and consequent long injection system lengths for velocities that are needed for direct drive targets). However, they are not practical for the higher mass of the indirect drive target.

Magnetic Injectors. Magnetic systems are capable of accelerating both direct drive and indirect drive targets with an appropriate sabot. An "iron-insert" type of magnetic injector can meet all the stated experimental system requirements except acceleration. Field saturation of the iron insert limits acceleration to ~1000 g's. In addition, since the iron is attracted along the bore by a series of solenoids, there is a large destabilizing force that tends to displace the projectile radially. Only mechanical contact can restrain the projectile. The resultant barrel wear pattern and its effect on injection accuracy is a concern.

A magnetic injector utilizing a high-temperature superconductor insert could be designed to reach the required velocities. It is estimated that accelerations of 300 g's could be achieved with currently available superconductor materials and accelerations of ~1200 g's can be achieved with materials expected to be available over the next several years. This type of system has the advantages of operating cryogenically and having the potential for a non-contacting operation (i.e., self-centering magnetically without physical contact with a "barrel"). This approach should be re-evaluated in the next few years, as improved performance high-temperature superconductors become available. A magnetic induction injector using aluminum or copper as the conducting material is another variation of this approach which should allow the required accelerations. Such a system would require significant development to implement for this experimental program, including construction of a reduced-size prototype to validate the design concept prior to construction of the test system.

Recommended Injector and Tracking Options. The primary purpose of this experimental program is to provide relevant data demonstrating target survivability under representative operating conditions and to provide moving projectiles to test and develop the target tracking system. This objective leads to selection of a mature and simple injector technology and a program to acquire the maximum amount of experimental data with regard to target survival and injection and tracking performance. It is also important to provide the maximum amount of versatility in the experimental injector, as the ultimate IFE target design and the final target injection requirements are unknown.

The light gas gun is recommended as the experimental injection system. This technology is expected to meet all requirements for target mass, velocity, acceleration, and heating –

both for this experimental program and for an IFE power plant. An additional advantage is that the simplicity and cost of the gas gun injector allows construction of a full-length system for demonstration of target injection within the framework of this test program. This system should be an effective prototype for the target injection equipment required for high-repetition target injection for the IRE.

In the event that the gas from the gas gun results in unforeseen and unsolvable difficulties in actual use, it is also recommended that the magnetic induction system be evaluated as a backup. This includes construction of a short proof-of-principal system with a metallic conductor in Phase I to verify the design concept.

The required tracking accuracy for both direct and indirect drive targets can be achieved with multiple photodiodes that are smaller than previously utilized and laser light sources. Due to the more rigorous accuracy specification for direct drive targets, temperature control of the detector mounting structures may be required.

Costs and Schedule

This workscope is identified as Task 3 of the IFE Technology Element of the OFES-VLT. GA is the lead organization for target injection and tracking, and is supported by LANL. The proposed program is broken down into subtasks that follow the key feasibility issues. The Phase I cost associated with each subtask is summarized below.

Estimated Costs for Phase I Target Injection and Tracking Activities

3.0 Target Technology R&D	FY00		FY01		FY02		FY03	
	LANL	GA	LANL	GA	LANL	GA	LANL	GA
3.2 Target Injection and Tracking R&D	200	1300	350	1250	500	1300	500	1300
3.2.1 Target Thermal Response	200		200		200		200	
3.2.2 Target Injection Accuracy and Tracking		650		625		650		650
3.2.3 Target Acceleration Response		650		625		650		650
3.2.4 Target Property Measurements			150		300		300	
Injection and Tracking Totals	1500		1600		1800		1800	

The schedule and logic of the experimental program described herein is summarized in Fig. Ex-1. Measurements of DT properties and initial determination of the effects of thermal radiation on filled and layered (stationary) targets takes place in Phase I, along with the design and initial operation of the new injection system. Upgrade of the injector system to incorporate injection of a cryogenically layered target takes place in Phase II, followed by a full demonstration of injection of cryogenic targets into a hot chamber.

Fig. Ex-1. IFE injection and tracking development overview.

1. Introduction and Background

1.1. Purpose and Scope

The primary near term goal of the US Inertial Fusion Energy (IFE) program (Phase I, present to 2003) is to develop the design basis for a Proof-of-Performance-level Integrated Research Experiment (IRE) (Fig. 1-1), which will explore the integration of driver beam and other systems with injection and tracking of prototypical IFE targets. Completion of the Phase I activities is a prerequisite to an IRE. Operation of the IRE, plus experiments with the National Ignition Facility (NIF), are prerequisites to a high-pulse rate Engineering Test Facility (ETF), and subsequently a DEMO plant leading to a commercial fusion power plant.

Fig. 1-1. Overview of IFE Development Strategy

This document provides an Experimental Plan for target injection and tracking activities primarily in Phase I (from the present to 2003). A brief discussion of Phase II activities is also included. This report includes activities taking place under the auspices of both General Atomics (GA) and Los Alamos National Laboratory (LANL). Specifically, GA and LANL have formed a target injection and fabrication development team to ensure existing facilities and expertise from the Inertial Confinement Fusion (ICF) program are leveraged to the maximum extent for IFE activities.

This document includes a brief review of previous work in the area of IFE target injection and tracking, and presents a plan for future work. The selection of a target injector technology is described, along with cost and schedule estimates to construct and operate an injector for demonstration purposes. Experimental measurements are described that are needed to address the critical issues for this area.

1.2. Background

In an IFE power plant, driver beams deliver an intense pulse of energy to a target containing cryogenic DT fuel. The energy pulse causes the fuel capsule to implode and initiates fusion reactions. To achieve high gain implosions, the targets must reach target chamber center with a symmetric layer of DT ice at ~ 18.5 K and with a smooth ice surface finish.

For direct drive IFE, the target consists of a spherical capsule that contains the DT fuel. Direct drive targets are the base-case option for laser-driven IFE. The SOMBRERO design (Ref. 1-1), with a dry-wall chamber, is the reference case for consideration of target injection and tracking demonstration experiments. However, the 0.5 Torr xenon gas used in that design to protect the chamber walls from heating, appears to cause excessive heating of modern direct-drive targets. The chamber gas also would make adequate position prediction much more difficult.

For indirect drive IFE, the capsule is contained within a hohlraum which converts the incident driver energy into x-rays to drive the capsule. Indirect drive targets are the base-case option for Heavy Ion Fusion (HIF). Power plant designs with liquid wall chambers, such as HYLIFE-II (Ref. 1-2) are the basis for proposed injection and tracking experiments with indirect drive targets.

The "Target Factory" at an inertial fusion power plant must produce, load with deuterium-tritium fuel, and deliver to the target chamber about 2×10^8 targets each year (approximately 500,000 per day) for injection at a rate of 5-7 Hz. This must be done with high precision of manufacture, high reliability of delivery and for an acceptable manufacturing cost. This challenge does appear to be achievable, but will require a significant development program.

In this chapter, critical issues for target injection and tracking are discussed, along with the current state of the technology and the proposed development plan to address these issues. More details of the proposed experimental plan are presented in Section 5.

1.3. R&D Approach

Significant overlap exists between the technology development programs for heavy ion and laser-driven inertial fusion in the areas of target fabrication, tritium fueling, cryogenic handling, high-rate injection, and tracking. Many of the material and equipment development needs are closely related, if not identical. Thus, an integrated R&D program has been developed to address these areas for both heavy ion and laser-driven IFE.

Even more essential for a successful development program, is a close integration of the target fabrication and injection/tracking development tasks, coupled with an ongoing coordination with IFE target designers. This is the primary reason why target fabrication, injection, and tracking is being addressed by a GA/LANL team with capabilities and experience in the relevant areas. Both GA and LANL have significant capabilities in the area of target fabrication from the ICF and other DOE programs. LANL has the facilities and equipment necessary for handling DT fuel, conducting fundamental property measurements on DT, and filling of capsules with DT. GA is the target support contractor responsible for target fabrication and deliveries to the five ICF laboratories [Los Alamos National Laboratory, Lawrence Livermore National Laboratory (LLNL), Sandia National Laboratories — Albuquerque, Naval Research Laboratory, and the University of Rochester). GA and LANL, working as a team, have conducted a similar development program to design and construct the OMEGA cryogenic target system for the University of Rochester. This system is the most advanced direct drive inertial fusion cryogenic target filling and handling system to date, with the first cryogenic shots (with D₂) scheduled for late 1999. GA and LANL are currently working together to design prototypes for the National Ignition Facility (NIF) target supply system. This system will provide filled, cryogenic, indirect-drive targets for implosion shots on NIF in FY05.

It isn't enough that a target, designed for high-gain energy production, can be produced economically. It must also lend itself towards economic and practical filling and layering, and it must survive injection into the target chamber without degradation. In the case of permeation filling (where the capsule is subjected to an overpressure of DT to drive the gas through the capsule wall), the strength of the capsule dictates the maximum overpressure that can be used and thus the time required for filling. The strength of the target may limit the acceleration to which it may be subjected during injection. The velocity at which the target enters the chamber must be sufficient to prevent overheating and degradation of the fuel layer by thermal radiation from the chamber walls. With respect to heating by thermal radiation, the required velocity increases with the emissivity of the target (the ratio of energy adsorbed by the target to the total thermal energy flux).

With respect to heating by gas in the chamber,* the heat flux increases with increased velocity, but the reduced time of exposure partially compensates for the increased surface heat load. The maximum permissible acceleration decreases for targets with thin walls or weak wall materials. This results in a greater distance for acceleration, greater injector length and increased cost for the acceleration portion of the target injector. Indeed, all the ramifications of the target design on the systems of the various IFE power plant approaches must be considered.

Thus, the approach of the development program is to work towards a self-consistent scenario demonstrating an economic path towards manufacturing, filling and layering, injecting, and tracking IFE targets. This must not only consider the direct cost of mass-producing IFE targets, but also the impact of the target design on the injection system, other related systems, and the total plant cost. Trade-offs can also be made between target characteristics and driver requirements: targets fabricated to extremely precise specifications may be more expensive but require less driver power. Ultimately, the optimal IFE target design must consider a multitude of factors, many of which must yet be enumerated, and will involve the whole IFE community.

Finally, this program is designed to take maximum advantage of the existing infrastructure, equipment, scientific data base, and methods that have been developed for ICF target fabrication, fuel filling, layering, and handling. The plan emphasizes needs that are unique to IFE, providing R&D that is not covered within the ICF program. Structuring tasks to build upon and leverage the ICF program is intended to provide a nationally cost-effective and coordinated program in these development areas.

1.4. Critical Issues for IFE Injection and Tracking

Significant overlap exists between the technology development programs for heavy ion and laser-driven inertial fusion in the areas of target fabrication, tritium fueling, cryogenic handling, high-rate injection, and tracking. Many of the material and equipment development needs are closely related, if not identical. Thus, an integrated R&D program has been developed to address these areas for both heavy ion and laser-driven IFE.

There are a number of critical issues associated with heavy ion and with laser-driven IFE. The top-level critical issue for target injection and tracking is the ability to accurately and repeatedly place a target filled with DT ice at ~18.5K, and meeting precise geometric requirements, at the center of a high-temperature target chamber at a rate of 5 to 7 Hz.

There are a number of considerations that make target injection more difficult for laser-driven (direct drive) targets as compared to heavy ion driven (indirect drive) targets. The hohlraum of the indirect drive targets acts to protect the cryogenic capsule and the capsule surface during injection into the hot target chamber. Driver beams must hit direct

*For example, the SOMBRERO reference chamber design includes 0.5 Torr of Xe fill gas.

drive targets with an order of magnitude greater accuracy than indirect drive targets. In addition, the symmetry requirements on the capsule and the surface finish requirements on the DT ice surface are more lenient for indirect drive targets (due to the smoothing effect of re-radiation by the hohlraum). Thus, direct drive targets must be evaluated more critically in terms of target thermal protection, injection velocity, accuracy required, and the ability of the target to withstand high accelerations.

Table 1-1 highlights the target injection and tracking issues for both ion and laser-driven IFE that are to be addressed in the Phase I R&D program (through FY03). Developing a credible pathway for success in these areas is vital to support the candidacy of these approaches to fusion energy.

Table 1-1
Highlights of Critical Issues for Target Injection and Tracking During Phase I

IFE Approach	Target Injection & Tracking
Laser-Driven Fusion (Direct Drive Targets)	<ol style="list-style-type: none"> 1) Ability of targets to survive the chamber environment (target heating due to radiation and chamber gases) 2) Accuracy and repeatability of target injection and tracking (ability to provide suitable beam steering and/or target steering, and shot-timing signals) 3) Ability of targets to withstand acceleration into the chamber (strength of target components, including the DT itself).
Heavy Ion Fusion (Indirect Drive Targets)	<ol style="list-style-type: none"> 1) Ability of the target to withstand acceleration during injection 2) Demonstration of ability to inject and track targets to required accuracy with prototypical injector technology 3) Develop concept for maintaining capsule temperature prior to injection

1.5. References

- 1-1 "OSIRIS and SOMBRERO Inertial Fusion Power Plant Designs," Final Report, March 1992, WJSA-92-01, DOE/ER/54100-1.
- 1-2 R.W. Moir et al., "HYLIFE-II: A Molten-Salt Inertial Fusion Energy Power Plant Design – Final Report," Fusion Technology **25**, 5 (1994).

2. Background and Previous Work

2.1. Reactor Design Studies

The SOMBRERO Inertial Fusion Energy (IFE) power plant (Ref. 2-1) provides one set of perhaps “most-challenging” conditions for developing target injection and tracking experiments. The SOMBRERO design is for laser-driven direct drive targets, and uses a very high temperature ($\sim 1485^{\circ}\text{C}$) dry-wall target chamber design. This dry-wall chamber utilizes a fill gas of 0.5 Torr* xenon to attenuate x-rays and re-radiate them over a longer time, controlling wall temperature excursions and wall ablation. This fill gas affects the target position along the direction of motion, greatly increases the heat flux to the target (compared to thermal radiation only), and induces an asymmetric heating profile on the injected target.

Key SOMBRERO plant parameters related to target injection and survival are summarized briefly below.

The targets described in the SOMBRERO study are CH polymer capsules of 5–6 mm outer diameter, with a wall thickness of 30–50 μm . There is approximately 4 mg of DT in each target ($\sim 150 \mu\text{m}$ thick DT layer). The effective first wall temperature seen by the injected targets is about 1485°C . The target is accelerated at $\sim 130 \text{ g}$'s to a final velocity of about 150 m/s. The accelerator length is 9 m, the sabot removal area is 2.5 m, the tracking length is 7.5 m, followed by 7.5 m to target chamber center (a total of 26.5 m). The reference SOMBRERO design assumes a gas gun accelerator is used with the capsule encased in a protective sabot and injected at an initial temperature of $\sim 4 \text{ K}$. Target-tracking and beam-pointing systems must match the center of the beams and the center of the injected target to approximately 0.3 mm.†

It was assumed in the SOMBRERO design that the target could be injected at $\sim 4 \text{ K}$ and could heat up to 21 K during its transit across the chamber. The most recent target designs indicate that the target must be shot at $\sim 18.5 \text{ K}$. In addition, recent work by LLNL and LANL show that a layered target can only sustain a temperature change of $\sim 0.5 \text{ K}$ without significant increases in ice layer roughness that would degrade target performance significantly. One parameter that can potentially reduce target heating is a higher injection velocity. This translates to higher acceleration forces during injection and the need to evaluate accelerator technologies in addition to the gas gun.

*The value of 0.5 Torr quoted is for standard temperature; this corresponds to about 3 Torr at the chamber temperature of 1758 K.

†Current accuracy requirements for direct drive targets are $\sim 0.02 \text{ mm}$.

The HYLIFE-II heavy ion beam driven power plant (Ref. 2-2) utilizes an indirect-drive target consisting of a cryogenic DT capsule inside a hohlraum as illustrated in Fig. 2-1. Target tracking and beam pointing requirements are approximately 0.4 mm.* The reaction chamber employs a liquid coolant/breeder (Flibe, a mixture of LiF and BeF₂) which has an operating temperature of about 900 K. The indirect drive targets must be injected and positioned at target chamber center in a specific orientation, unlike the spherical direct drive targets. However, the hohlraum provides significant protection for the cryogenic capsule and the surface quality requirements of the capsule are expected to be less for indirect-drive targets.

Fig. 2-1. Depiction of recently proposed heavy ion beam driven, indirect drive target design.

2.2. Experimental Status

A gas gun indirect drive target injection experiment has been constructed and operated at LBNL (Ref. 2-3). This experiment has provided the most comprehensive and relevant experimental data available on target injection and tracking. It is directed primarily at indirect drive targets, but experiments with projectiles simulating direct drive targets have also been conducted. Additional details of the experimental setup and characteristics of this work can be found in Appendix A.

Many of the significant components of this experiment are illustrated in Fig. 2-2. The major components from right to left include the gas reservoir with pressure gauge, the gas valve, the small vacuum tank which encloses the revolver loading mechanism, the gun barrel, the vacuum piping for the target to drift through which holds the first two detectors, the vacuum pump, and the large vacuum tank which holds the final detector and the target catcher.

IFE indirect-drive target-sized projectiles (to simulate targets) are loaded into a revolver. The vacuum pump removes air from the system downstream of the gas valve. A micro-stepping motor aligns a target with the gun barrel. The gas valve is opened for approximately 50 ms to allow 1.1 atm (16 psia) gas from the 0.1 m³ gas reservoir to accelerate the target down the gun barrel. The target and gun barrel are both rifled to

*Current accuracy requirements for indirect drive targets are ~0.2 mm.

Fig. 2-2. Schematic drawing of the experimental layout.

minimize target tumble. The target passes through three detectors. The first detector is for timing purposes only, but the remaining two measure the target's position in all three dimensions. The detectors use light emitting diodes on one side of the target motion and photodiodes on the other side to detect target position. In addition, the amount of tumble of the targets can be measured as they pass the detector. Based on the position of the target as it passes the second detector, and the time required to pass between the first two detectors, when and where the target will pass the third detector is predicted. The predictions are then compared to the measured position of the target at the third detector.

Table 2-1 shows the experimental results and those results extrapolated to power plant dimensions. The results show that relatively simple gas gun technology can repeatedly inject a simulated indirect drive target to within about 7 mm of the driver focus point, easily within the range of laser or beam steering mechanisms to hit, but not sufficient to avoid the need for beam steering. Photodiode detector technology was adequate to detect the target position with sufficient accuracy that the driver beams should be able to achieve the approximately $\pm 200 \mu\text{m}$ accuracy needed. Less accuracy is required along the direction of target motion ($\sigma_{\Delta z}$) because the driver beams are coming in from small angles along that direction.

Table 2-1
Experimental vs. Extrapolated Power Plant Parameters

Quantity	Description	Power Plant (mm)	Experiment (mm)
	Distance from gun	7500	3000
x	Horizontal position		
y	Vertical position		
z	Position along target path		
σ	Standard deviation at detector 3 (chamber center)		
σ_x	σ (horizontal target position)	4.72	1.89
σ_y	σ (vertical target position)	4.75	1.90
σ_{xy}	$\sqrt{\sigma_x^2 + \sigma_y^2}$	6.70	2.68
$\sigma_{\Delta x}$	σ (horizontal target prediction position error)	0.160	0.101
$\sigma_{\Delta y}$	σ (vertical target prediction position error)	0.115	0.073
$\sigma_{\Delta x(\text{tumble})}$	σ (horizontal target end position error due to tumble)	0.068	0.068
$\sigma_{\Delta y(\text{tumble})}$	σ (vertical target end position error due to tumble)	0.074	0.074
$\sigma_{\Delta xy(\text{total})}$	$\sqrt{\sigma_{\Delta x}^2 + \sigma_{\Delta y}^2 + \sigma_{\Delta x(\text{tumble})}^2 + \sigma_{\Delta y(\text{tumble})}^2}$	0.22	0.16
$\sigma_{\Delta z}$	σ (target prediction position error due to timing)	0.3	0.2

Additional experiments with projectiles to simulate direct drive targets (spheres) were conducted with the apparatus described above. As illustrated in Fig. 2-3, a 0.635 cm (0.25 in.) diameter solid plastic sphere was propelled in a 1.6 cm (0.631 in.) diameter aluminum and delrin sabot. A 0.71 cm (0.28 in.) diameter 0.5 cm (0.2 in.) deep cup with cone is cut into the leading edge of the sabot. A 5 mm (0.002 in.) lip is pressed into the leading edge of the cup to keep the sphere from rolling out. During acceleration the sphere rolls back into the cone. A 2.0 cm (0.8 in.) inner diameter 1 cm (0.4 in.) square cross-section coil was mounted to the forward end of the first detector box. After the sabot leaves the gas gun barrel and passes the first target detector, the magnet is pulsed to produce a 0.5 Tesla dipole field. This field slows the aluminum sabot's speed about 1 m/s from a speed of approximately 70 m/s allowing the plastic sphere to move ahead. Thirteen sabots with spheres were shot and the magnet was pulsed 10 of those times. When the magnet was not pulsed, the sphere stayed in the sabot. When the magnet was set to pulse, the sphere came out of the sabot 9 out of 10 times leading the sabot by about 200 microseconds or 1.4 cm at the second detector. For unknown reasons the sphere did not come out on one shot. When the sphere did come out of the rapidly spinning sabot, the accuracy of the sphere placement was about the same as the sabot or targets that were previously injected. The sphere position standard deviation at detector 2 over 1 m away was 0.8 mm horizontal and 0.7 mm vertical.

Fig. 2-3. Sabot/sphere configuration used for direct-drive target runs.

2.3. Current Target Designs

The ultimate IFE target design to be utilized in a power plant is unknown today and will be the result of an ongoing iteration between those involved in target design, target fabrication, target injection/tracking, plant design, and safety and environmental work. Currently proposed IFE target designs are described briefly here for the purpose of assessing relevant target injection and tracking system requirements and evaluating proposed injection methods.

2.3.1. Direct Drive

Figure 2-4 illustrates the concepts for three recently proposed target designs under consideration for ignition and/or high gain (Ref. 2-4). The shock heated DT ablator is shown in Fig. 2-4(a). Assessments indicate it would achieve ignition but has gains too low for energy production applications. Figure 2-4(b) illustrates the shock preheated foam ablator target design. Its exterior coating of very low density foam is thought to be impractical for the handling and accelerations required for injection. Figure 2-4(c) illustrates the radiation preheated target design, which has the potential for gains high enough for energy applications. It also contains low-density CH foam, but this target design has the foam on the inside of the capsule and filled with DT. As a first approximation, the DT fill can be expected to provide strength to the foam comparable to solid DT. The target capsule is doped with tungsten or gold, which generates the x-ray preheating. This high-Z coating on the surface is compatible with making the target surface highly reflective to reduce its heating by thermal radiation during the injection process. The total target diameter is about 4 mm. The ability to inject this target through a 0.5 Torr xenon fill gas in a SOMBRERO-like target chamber remains a concern.

Fig. 2-4. Three recently proposed target designs for the direct-drive laser fusion program

2.3.2. Indirect Drive

Figure 2-1 provides a schematic illustration of a recently proposed heavy-ion driven, indirect-drive target design (Ref. 2-5). The capsule is a beryllium ablator doped with bromine, the hohlraum is a mixture of gold and gadolinium, and there are a number of convertors, beam blocks, and end shields consisting of gold, gadolinium, iron, aluminum, and plastic. The hohlraum diameter is approximately 13 mm. While iterations on the materials of construction and the ability to economically fabricate and assemble this target remain, this target provides gains suitable for energy applications.

2.4. References

- 2-1 "OSIRIS and SOMBRERO Inertial Fusion Power Plant Designs," Final Report, March 1992, WJSA-92-01, DOE/ER/54100-1.
- 2-2 R.W. Moir et al., "HYLIFE-II: A Molten-Salt Inertial Fusion Energy Power Plant Design – Final Report," *Fusion Technology* **25**, 5 (1994).
- 2-3 R.W. Petzoldt, "IFE Target Injection and Tracking Experiment," *Fusion Technology* **34**, 831 (1998).
- 2-4 S.E. Bodner, "Status of Direct-Drive Laser Fusion Target Designs," *Proc. of the 17th IEEE/NPSS Symposium on Fusion Engineering*, San Diego, California, Vol. 2, (Institute of Electrical and Electronics Engineers, Inc., Piscataway, New Jersey) p. 588.
- 2-5 D.A. Callahan-Miller, and M. Tabak, "Increasing the Coupling Efficiency in a Heavy-Ion Inertial Confinement Fusion Target," Lawrence Livermore National Laboratory Report LLNL UCRL-JC-133231 (1999).

3. Selection of a Target Injection System

3.1. Target Injection Considerations

3.1.1. Kinematic Relationships (Acceleration, Velocity, Distance, Rate)

As indicated in Fig. 3-1, a target is propelled by an injector of length L_α with acceleration α to attain velocity v . The target then coasts a distance L_c to the center of a reaction chamber of radius r_c . The time that the target spends in the reaction chamber t_r is limited to some fraction of the time between shots leaving time for chamber clearing. The in-chamber time is also limited due to heating of the cryogenic target inside the hot chamber. This sets a lower limit on target speed, $v \geq r_c / t_r$. For constant acceleration, the acceleration time is given by $t_\alpha = v / \alpha$. For systems capable of accelerating only one target at a time, the acceleration time must be less than the interpulse time t_i . This can place an upper limit on target velocity ($v < \alpha t_i$). The acceleration is limited by the stress it places on the relatively fragile targets. The accelerator length is given by $L_\alpha = v^2 / 2\alpha$. These relationships are covered in more detail in Ref. 3-1.

Fig. 3-1. Target kinematic relationships.

3.1.2. Target Heating

To achieve high gain implosion, the targets (both direct drive and indirect drive) must reach the target chamber center with a symmetric layer of DT ice at ~ 18.5 K and with a sufficiently smooth ice surface finish. While previous studies have generally assumed that a filled, layered target could be cooled to temperatures of 5–10 K prior to the shot, recent data indicate the target may be limited to a temperature change of ~ 0.5 K before DT ice surface finish degradation significantly reduces target gain. Similar temperature change limitations appear to apply to both warming of the target (Appendix B) and cooling (Ref. 3-2). This means that target heating during injection is a critical feasibility issue and an important design driver for laser-driven IFE. This issue impacts the target

design, the chamber design, the chamber-protection method, and drives the requirements for the injection system. Heavy ion beam driven IFE targets (indirect drive) include a surrounding hohlraum that offers additional protection to the DT fuel capsule and lessens these effects.

3.1.2.1. Direct Drive Targets. The issue of target heating is a critical one for direct drive targets for attaining an acceptable combination of injection velocity and acceleration, target design, and target performance. There are several fundamental temperature-related “limits” that are discussed below for the direct-drive IFE target. The current strategy is to take each of the “limits” and look for potential pathways to avoid them, or push their boundaries to the point where they are no longer limiting. The objective of this discussion is to show that there are pathways for success; the objective of the experimental program planned for the next few years is to prove it can be done.

The first “limit” is closely related to target heating and is an issue that must be solved by the ICF program (and which impacts IFE target injection). This is the ability to cool targets from their layering temperature to the desired shot (or injection) temperature. Careful beta-layering experiments in spherical geometries at LLNL have demonstrated the capability to achieve inner DT ice surface finishes of slightly less than 2 μm RMS* at just below the triple point ($\sim 19.7^\circ$). However, when cooled more than ~ 0.5 K there is a sharp increase in the DT surface finish, reaching about 4 μm RMS at 19 K. The ICF program is developing methods to cool the target to ~ 18.5 K. The objective for the IFE program that is assumed in this document is to extend the methods developed for ICF to allow further cooling to ~ 18 K. This provides a margin of ~ 0.5 K for heating of the IFE targets during injection. Another potential IFE pathway is to develop target designs that can tolerate a rougher ice surface finish. The ICF program is seeking values of about 1 μm RMS. Some layering experiments have measured surface finishes in the range of 4–6 microns at temperatures as low as 18.2 K. It may be possible to tolerate these surface finish values for mode numbers greater than ten. Further iteration between target design and injection is needed to develop this pathway.

The second “limit” for direct drive target injection involves the heating of the target during its transit across the target chamber and the consequent effects on the target characteristics. The design of the SOMBRERO target chamber incorporates 0.5 Torr of xenon gas to reduce the rate at which x-ray and plasma energy is deposited on the chamber wall surface. During injection into the chamber, the cryogenic target is heated by interaction with this target chamber gas and by thermal radiation from the target chamber, which has an effective first wall temperature of $\sim 1485^\circ\text{C}$. With high-reflectivity targets† (and a chamber filled with 0.5 Torr of gas) the majority of the target heating will

*Root mean square surface finish. Current ICF target designs call for ice layers smooth to about 1 μm RMS.

†Target surface reflectivity of about 99% is believed achievable with targets such as the radiation preheated design (with a capsule doped with tungsten or gold) described in Section 2.3.1.

be from the gas rather than the radiation.

Figure 3–2 shows an ANSYS calculation of temperature rise on the ablator surface of the radiation preheated direct drive target [i.e., the target shown in Fig. 2–3(c)] as a function of total heat flux and injection velocity (Appendix D). The injection velocity determines the time of exposure to the heat flux. Also shown on this figure is the “allowable” temperature change (~ 0.5 K) that has been estimated (Appendices B and C). Also, there is considerable uncertainty in interpreting the calculations of stress buildup in the DT ice. It is not known that reaching the yield stress necessarily results in cracking or surface roughening — or whether such degradation happens on the time scales involved here (about 20 ms for ~ 400 m/s injection velocity). Thus, it may well be possible for a target to exceed this “limit” of reaching the yield stress under injection conditions.

Fig. 3–2. Temperature rise of ablator surface during injection.

Assuming methods can be found and demonstrated to extend the temperature change “limits” beyond the realm of ~ 0.5 to 1 K δT , the next “limit” is probably reached during injection when the outer DT layer starts to vaporize. One would expect that the resultant bubble formation on the vaporizing surface would cause implosion instabilities. Again, kinetics may be on our side; there may be superheated liquid (at the surface) in the 20 ms time frames under consideration. At 22 K the DT vapor pressure is 47 kPa (~ 0.5 atm); it's unlikely that pressures greater than this value could be maintained by the polymer shell, and vaporization and bubbles would result. This may represent a true maximum temperature limit for the target. In any event, it appears from Fig. 3–2 that heat fluxes less than about 1 to 2 W/cm^2 will be required for a target to reach the chamber center in a usable state.

As for the degree of target heating, several heat sources must be dealt with. The self heating due to beta decay of the DT fuel will cause a temperature rise of about 0.06 K/s, if the heat is not conducted away. Given in-flight times of about 20 ms this is not a significant contributor. However, radiation heating in the reaction chamber is an important heat source even with a target reflectivity* of 99%. The resulting surface heat load from thermal radiation on such a target is about 0.5 W/cm^2 .

The heat flux from gas-heating depends upon the velocity and the pressure of the gas in the chamber. While reliable, verified models for the specific conditions of the cryogenic target in a high-temperature, reduced-pressure, gas-filled chamber are not available, we have attempted to estimate the total heat load (radiation plus gas-heating) as shown in Fig. 3-3. These curves were calculated using a standard equation for heat transfer to a sphere in continuum regime (Appendix E). It appears that heat fluxes of less than 1 to 2 W/cm^2 can only be achieved at very low xenon gas pressures. Injection into a chamber with the reference gas pressure results in heat loads that are an order of magnitude higher than required. (Another model for heat transfer to the target during injection is given in Appendix F. This model represents an upper limit to heating, as it assumes that all gas molecules swept out by the target during its transit stick to the cryogenic surface and transfer all of their energy to the target.)

Fig. 3-3. Calculated heat flux on target under varying chamber conditions and injection velocities.

*There is the potential of reducing the reflectivity of the target by condensation of the chamber gases on the target surface. However, we think this is unlikely to be a serious effect. Each xenon atom will come in with a very large energy and substantially heat its impact site. It takes some time to dissipate that energy into the volume of the shell — meanwhile there is only a very small interaction between the atom and the surface. Thus, we suspect the xenon atoms will either promptly evaporate from the surface or be knocked off the surface by subsequent incoming atoms.

Figures 3–2 and 3–3 make the simple assumption that the heat flux imposed on the target is symmetrically distributed over the entire surface area of the target. This is not true for heat loads resulting from chamber gases. An initial attempt to evaluate the asymmetry of the surface heat flux from xenon gas at 0.5 Torr is also given in Appendix D. Depending on the velocity of injection, the heat load at the leading edge of the target can be several times higher than the heat load near the trailing edge. This is an important effect that must be further evaluated.

It must be emphasized that the data presented here are preliminary calculations. Reliable, verified models for the gas-heating of a target under the range of pressure, temperature, and injection velocity regimes expected will be developed as an inherent part of the testing program over the next few years. It is also not known with certainty what temperature profile will be required to avoid changes in the target that would reduce target gain. These are important areas for near term study for injection of direct-drive targets.

3.1.2.2. Indirect Drive Targets. Reference 3–1 reported finite element calculations of heat transfer into a glass-encased indirect-drive target (Ref. 3–3) as a function of time. The analysis assumed the target was injected at 17.3 K and that a 0.2 K temperature rise was allowed. The calculated outer fuel temperature reached 17.5 K after 350 ms of exposure to a heat flux of 5.0 W/cm^2 . Appendix G reports more recent calculations using a current target design. It shows negligible heating of a target as it is propelled through a gas gun and into a hot chamber environment. These calculations point out that indirect-drive targets heat much more slowly than direct-drive targets, and injection velocities of $\sim 100 \text{ m/s}$ are entirely adequate from the standpoint of DT heating.

3.1.3. Target Chamber Interfaces (Radiation/Neutron Shielding, Flibe Entrance)

3.1.3.1. Neutron Shielding. One need for neutron shielding is to protect the target DT from neutron heating. Reference 3–1 shows that the neutron induced heating of target DT exposed to a 400 MJ yield 8 m away would be about 1 K. To reduce this heating to 0.1 K would require about 2 transport mean free paths of shielding which corresponds to about 14 cm of iron. If there were more targets waiting to be shot at about that distance, additional shielding would be required for them.

The gun barrel (and adjacent components) may also require additional neutron shielding. The limiting neutron irradiation effect will probably be swelling. Swelling will cause the inner diameter of the barrel to increase by about one third of the volumetric swelling. If the diameter of the barrel must be constant to 0.1% for example, the maximum allowed swelling is 0.3%. Reference 3–4 considered three structural materials for a fusion energy blanket. The swelling estimates provided for those materials can give us an idea of the expected swelling that would occur in a gun barrel in a neutron environment. The three materials were prime candidate allow (PCA) austenitic steel, HT-9 ferritic steel, and

V-15Cr-5Ti. All three undergo about 11 displacements per atom (dpa)/MW-y/m². The PCA swelling is about 0.02%/dpa up to 100 dpa after which the swelling is about 0.5% per dpa. Up to 100 dpa, the HT-9 swells about three times less and the vanadium alloy swells about six times less than PCA. Based on the above estimates if swelling must be limited to 0.3% with HT-9, the maximum dpa is 45 which corresponds to about 4 MW-y exposure. Assuming 2,000 MW neutron power and an 8 m radius to the gun barrel without neutron shielding, the swelling limit would be exceeded in 1.6 years. Shielding which would reduce this neutron load by a factor of 20, could allow barrel swelling to be acceptable for 30 years. Although this is a very rough estimate of neutron shielding requirements, provision of adequate neutron shielding to minimize target heating and barrel swelling does not appear to be a feasibility concern.

3.1.3.2. Flibe Diffusion. The HYLIFE-II reactor has Flibe with a molecular density of about $3 \times 10^{13}/\text{cm}^3$. We will need a 1 cm radius opening for the targets to pass into the reaction chamber.

It is desirable to keep Flibe vapor out of the target injection area, especially out of the gun barrel and off the target tracking surfaces. As indicated in Fig. 3-4, one way to achieve this would be to have a counterflow of gas in a tube leading to the chamber which would minimize Flibe diffusion. The end of the tube would be shut during pressure pulses from the fusion micro-explosions.

Appendix H calculates that the Flibe vapor can be prevented from entering the target-tracking region with an average gas flow of less than 1 mg/s. The shutter would be closed most of the time. It would be open just long enough (~5 ms) to allow the target to pass through and to allow helium to clear the Flibe vapor. The helium should not excessively affect target position accuracy.

Fig. 3-4. Flibe vapor entering the target injection and tracking area is minimized by: (1) closing a shutter during the fusion pressure pulse and (2) providing a counter flow of helium gas while the shutter is open.

3.1.4. Accuracy of Injection

3.1.4.1. Requirements. The requirements are to place a target to within ± 5 mm (for both indirect and direct drive), but to track the target and align the driver beams to within ± 20 μ m for direct drive and to within ± 200 μ m for indirect drive. This set of requirements is based on active driver beam steering; it may be more efficient to achieve the desired alignment for injection with target steering rather than driver beam pointing, thus reducing the overall system cost and complexity.

The more accurately targets can be placed in a reaction chamber, the less driver beams will have to be steered to hit the target and the less tracking system field of view will be required. We expect that driver beams can readily be steered ± 5 mm and that we'll be able to achieve this placement accuracy, especially in the case of indirect drive targets. This placement accuracy corresponds to about ± 0.3 mrad for direct-drive targets and ± 0.7 mrad for indirect-drive targets. The accuracy with which the centerline of the beams must be aligned with the centerline of the target is much greater, ± 0.2 mm for indirect drive and ± 0.02 mm for direct drive.

3.1.4.2. Direct Drive Target Injection Accuracy. Placement accuracy of ± 1 mrad has been achieved with solid plastic balls which were separated from an aluminum sabot. Injecting lower mass, cryogenic targets with this accuracy has not yet been demonstrated. The ± 20 μ m target alignment accuracy requirement is an order of magnitude greater than has been achieved. Gas in the reaction chamber would make meeting the target position prediction requirement and alignment accuracy even more challenging.

A simplified calculation of expected target drag for a SOMBRERO target chamber can be performed assuming all gas molecules in the path are accelerated to the target speed. This assumption is equivalent to saying that the coefficient of drag is 1.0, which is true within about 5% for spheres at speeds above Mach 1.2 (Ref. 3–5). The coefficient of drag is less at lower speeds.

The mass of gas in the target path is

$$m = \pi r_t^2 \rho r_c = 3.14(0.2\text{cm})^2 (4 \times 10^{-3} \text{mg/cm}^3)(600\text{cm}) = 0.3\text{mg} \quad .$$

The mass of this gas is two orders of magnitude below the target's mass, but is still enough to significantly affect the target's trajectory.

Using conservation of momentum the fractional average change in the target's velocity is given by

$$\frac{\Delta v_{ave}}{v_{ave}} = \frac{v_i - v_f}{2v_{ave}} \approx \frac{m_g}{2(m_g + m_t)} = \frac{0.3}{8} = 0.0375 \quad .$$

The final change in the target's position along the direction of motion is then given by

$$l \frac{\Delta v_{ave}}{v_{ave}} = 600 \text{ cm}(0.0375) = 22 \text{ cm} \quad .$$

This is 4 orders of magnitude above our allowed position error and must be corrected for. This position error is repeatable to the extent that the gas pressure is the same from shot to shot. However, if the gas pressure is not consistent to >99.99%, additional correction is required. One method to correct for the target's position is to measure the target's position, a few cm before the target reaches the chamber center with a laser beam and photodiode detector, and trigger the driver laser beams based on this measurement.

To determine whether the gas flow around the target is turbulent or viscous, we check Reynolds number

$$R_e = \frac{\rho v R_b}{\mu_s} = \frac{4 \times 10^{-3} \text{ kg/m}^3 (1,000 \text{ m/s}) (2 \times 10^{-3} \text{ m})}{4 \times 10^{-5} \text{ kg/m} \cdot \text{s} (1800 \text{ K} / 500 \text{ K})} = 55 \ll 2300 \quad .$$

The gas viscosity was extrapolated from Ref. 3–6. Even considering shock compression of a factor of about ten, this flow will be viscous. There should be very little side load on the target, assuming that the speed of the bulk motion of the gas in the chamber is much less than the target speed. The lateral force on the target (and lateral target displacement) is roughly proportional to the bulk gas speed squared. If the “wind speed” in the chamber is greater than about $(0.0001)^{1/2}$ times the target speed, it will have a significant effect on the final target position. Significant gas motion is likely due to expansion and contraction as the gas is heated and cooled with each shot.

The mean free path of the xenon atoms is also of interest here. It is 5.6×10^{-5} mm at 750 Torr and 273 K (Ref. 3–6). Under reaction chamber conditions, the mean free path of the atoms is

$$\ell = \ell_o \frac{T}{T_o} \frac{P_o}{P} = 5.6 \times 10^{-5} \text{ mm} \left(\frac{1800 \text{ K}}{273 \text{ K}} \right) \left(\frac{750 \text{ Torr}}{3 \text{ Torr}} \right) = 0.1 \text{ mm} \quad .$$

This is a factor of 40 below the target diameter. We are not operating in the free molecular flow regime.

In summary, 0.5 Torr of xenon will affect target position along the direction of motion by approximately 20 cm. Because of this, a final target detector to detect target passing near the shot point for timing purposes may be required in a gas-filled target chamber. The flow of gas around the target is not turbulent and will probably not cause significant variation in target position perpendicular to intended injection path unless there is bulk gas motion (wind) in the chamber that exceeds about 1% of the target speed. This wind might be caused by expansion and contraction of the gas with shot heating and post-shot cooling.

3.1.4.2. Indirect Drive Target Injection Accuracy. Target placement accuracy (± 1 mrad) and position prediction (alignment) accuracy (± 0.1 mm at 3 m) approaching or exceeding that required for indirect drive targets has been achieved with room temperature target sized projectiles at LBNL (Ref. 3–7). Greater accuracy injection and position prediction capability at higher speed is desirable. Cryogenic injection still must be developed and demonstrated. Appendix I reviews the effect of residual Flibe droplets in the reaction chamber on target position accuracy including tumble effects. The mass of Flibe in the target path should be less (preferably much less) than about 1 mg.

3.1.5. Reliability

Target injection needs to be highly reliable to provide 500,000 targets per day to the reaction chamber while not requiring frequent unplanned maintenance shutdowns. It might be desirable to operate two target injection systems at half capacity, so that if one needs to be shut down, the other can provide targets at the full design repetition rate.

For most designs, if an occasional target were not correctly positioned, there would be no catastrophic failure. However, in some designs, the driver beam intensity is sufficient to damage the opposite wall of the chamber, or cause damage to the opposite beam lines, if the target does not block the beam path. Therefore, it is often important, not to trigger the driver beams if the target is not in the correct position. A reliable target tracking system must be used to trigger the driver beams.

3.2. Injector System Requirements

Requirements for an experimental target injector system were derived based on first reviewing the expected injector system requirements for a power plant. While it is not absolutely necessary that the injector used for an experimental program have the same technical basis as one that would be applicable to a production unit, this is a highly desirable attribute. This approach has the advantage of using the experimental injector as a test bed to learn the intricacies of operating a given technology under representative conditions.

The strategy for the experimental program is to inject representative targets at expected power plant velocities into a high-temperature chamber in order to simulate thermal exposures (i.e., times-of-flight) that would be encountered in a power plant. It is desirable (although not specified as a requirement) to construct a full-length injector system that will be able to reach the expected power plant velocities at accelerations that can be withstood by an actual target. For example, to accelerate a target to a velocity of 400 m/s while not exceeding 1000 g's requires an injector length of 8 m.

Some other factors that drive the high level injector requirements include: the type of target (direct or indirect), the temperature environment of the reaction chamber, the

physical space available for the accelerator, and the accuracy with which the target must exit the injector. The goal will be to ultimately design an injector system that is capable of meeting the future needs of Inertial Fusion Energy (IFE). To this end, we have considered accelerator systems capable of injecting both direct and indirect targets, at speeds that meet the upper end of the required range.

Table 3–1 summarizes the requirements that are necessary to achieve target injection into the reaction chamber. Two columns are shown; one is for a production fusion power plant injector, the other for an experimental facility. Direct drive target heating limitations increase requirements for injection velocities to ~400 m/s. With the exception of Items 2, 3, and 10, it is seen that no differences are expected between the production and experimental injectors. That is, the experiment should very closely simulate all the expected acceleration loads on targets. The targets have the same dimensions and mass as actual targets. A higher acceleration capability than is needed in the production injector is desired to enable us to test the limits of acceleration on target strength.

Table 3–1
Injector Requirements

Requirement/Parameter	Production Injector		Experimental Injector
	Direct Drive	Indirect Drive	
1. Target speed (m/s)	400	180	400 for direct drive* 180 for indirect drive
2. Acceleration (g's)	1500	1000	5000†
3. Repetition rate (Hz)	6	6	6 Multi-shot "burst" capability, upgradable to cryogenic
4. Target mass (g)	0.005 (without sabot)	1 (without supporting structure)	0.005 (direct drive) 1 (indirect drive)
5. Target diameter (mm)	5	12	5 (direct drive) 12 (indirect drive)
6. Target temperature range (K)	18–19	18–19	18–19 (Initial injections at room temperature)
7. Target spin (rotations per second)	300 (for sabot stabilization)	300‡	300
8. Target exit accuracy (mradians)	±0.3	±0.7	±0.3
9. Cryogenic fuel temperature rise (from start of injection to center of chamber) K	0.5	0.5	0.5
10. Availability (%)	>99	>99	Several shots per hour for experiments

* Capability of 400 m/s at 5000 g acceleration; implies a 1.6 m experimental minimum injector length.

† 5000 g capability chosen to include capability of driving targets to destruction.

‡ This is acceleration limited; 300 rps results in ~700 g's centrifugal acceleration on the DT.

Many aspects of target injection can be demonstrated with this relatively short single-shot test injector that is capable of accelerations that exceed those required for a power plant. This will allow full speed target tracking experiments to be carried out. Also desirable for a test facility is the ability to produce room temperature "burst mode" operation, where a pellet "bunch" (about 6 per bunch) is injected in a single burst into the test chamber.

One can spin the target to provide stability against tumbling, or possibly to remove a multi-section sabot. Good stability was achieved in the LBNL experiment (Ref. 3-7) with 1 revolution each 14 in. (about 3 revolutions per meter). One limit on the rotation rate is the centripetal acceleration of the fuel. For example, the 200 rev/s frequency attained in Ref. 3-7 at a speed of 70 m/s gives centripetal acceleration at 2 mm of

$$a = \omega^2 r = (2\pi f)^2 r = (6.28 \times 200 / s)^2 (0.002m) = 3155 m / s^2$$

This is enough acceleration to cause significant stress (not yet calculated) in the DT within a fuel capsule. If the capsule is off center in the hohlraum, there will be a centripetal force on the capsule which must be provided by the capsule support structure (membranes). For a capsule that is 50 microns off center, the centripetal acceleration is $79 m/s^2$ which is much less than the proposed linear acceleration. If the support membranes are pre-stressed to maintain a normal force on the leading and trailing surfaces of the capsule, then it should be possible to provide the required frictional force to keep the capsule from slipping toward the side of the hohlraum.

3.3. Target Injection Methods

3.3.1. Gas Guns

3.3.1.1. Gas Guns. IFE targets must be injected over a distance of several meters several times per second into a reaction chamber. The projectile velocity achieved by a gun is given by

$$v = \sqrt{2\bar{\alpha}L}$$

where $\bar{\alpha}$ is the average acceleration and L is the accelerator length. For a gas gun, neglecting friction and pressure in front of the projectile, this becomes,

$$v = \sqrt{\frac{2\bar{p}AL}{M}}$$

where $\bar{p}$ is the average pressure at the base of the projectile, A is the area of the projectile base, and M is the projectile mass. These equations clearly show the quantities on which target velocity depends and which can be varied to achieve high velocity.

Indirect drive targets can be injected at a speed on the order of 100 m/s which is very easily achieved with a gas gun 2 m long with gentle 250 g's acceleration. This speed is much less than the sound speed of light gases at room temperature. The sound speed for an ideal gas is given by

$$a = \sqrt{\frac{\gamma RT}{m}}$$

where γ is the ratio of specific heats, R is the ideal gas constant, T is the absolute gas temperature, and m is the gas molecular weight.

A low molecular weight high temperature gas has a high sound speed. The gas with the highest sound speed is H_2 which at 300 K has sound speed

$$a = \sqrt{\frac{\gamma RT}{m}} = \sqrt{\frac{1.45(8.31 \text{ J/mole} \cdot \text{K})(300 \text{ K})}{0.002 \text{ kg/mole}}} = 1345 \text{ m/s}$$

Helium at 300 K has sound speed 1011 m/s. Other gases have much lower sound speeds and are not as good for use as high-speed propellants.

Direct drive targets require injection speeds of about 400 m/s to minimize heating of a cryogenic target in a reaction chamber. As noted in Ref. 3–8, two stage gas guns have accelerated projectiles to over 10,000 m/s. High speed is normally achieved with very high pressure and acceleration. Since DT ice has a low yield stress especially at or above the current design temperature of 18 K, target injection acceleration will likely be limited to less than about 1000 g's. This could require a very long gun barrel. For example, to accelerate to 400 m/s requires acceleration distance

$$x_p = \frac{u_p^2}{2\alpha} = \frac{(400 \text{ m/s})^2}{2(10,000 \text{ m/s}^2)} = 8 \text{ m}$$

This assumes that constant acceleration can be achieved. To achieve constant acceleration, the pressure at the projectile must be constant which requires the pressure at the barrel inlet to increase with time. For target speed much less than the sound speed in the gas, the pressure drop from the barrel inlet to the target, might be negligible. However, this pressure drop is quite large for higher injection speeds even for an ideal gas and especially when friction is taken into account.

3.3.1.2. Constant Base Pressure Gun: Isentropic Expansion of Ideal Gas Without Friction. Reference 3–8 describes a gun that provides constant pressure to the projectile. This is a constant base pressure gun. Figure 3–5 illustrates such a gun with a piston driven gas in a pump tube chamber. Figure 3–6 shows a more practical method to achieve constant base pressure for this application. Separate high and low pressure reservoirs are

Fig. 3-5. A piston compresses gas in the chamber to provide constant pressure to the base of the projectile.

Fig. 3-6. The poppet valve opens to allow full gas flow from the low-pressure reservoir to start the projectile motion and stays open until higher pressure is required. Gas is then throttled from the high-pressure reservoir to achieve the desired chamber pressure increase later in the acceleration process.

used. The valve in line with the lower-pressure reservoir is opened to start the projectile moving. The valve in line with the higher-pressure reservoir is opened in a controlled manner to throttle gas flow and maintain the required barrel inlet pressure. The low-pressure valve is shut once the chamber pressure exceeds the low-pressure reservoir pressure. A cam arrangement similar to that described in Ref. 3-1 could potentially be used to control the valve positions.

Were it not for gas friction, a constant base pressure gun would allow us to achieve very high target velocities. However, as shown in the next section, gas friction is expected to limit the target injection speed to far below the sound speed.

3.3.1.3. Gas Friction Effects. There is friction of the gas flowing inside the gun barrel. The frictional force per unit mass of gas in the barrel is given in Ref. 3–8 as

$$\frac{F_{fg}}{m_g} = \frac{c_f u^2}{r_B}$$

where r_B is the barrel radius and c_f is the skin friction coefficient given by

$$c_f \approx 0.049 N_{Re}^{-1/5} \approx 0.005 \quad .$$

Since the gun length required to propel the projectile increases as speed squared, and the average gas density increases as the target speed increases, this frictional force increases faster than speed to the fourth power. As the frictional force becomes comparable to the force on the projectile, the gun performance is degraded and greater inlet gas pressure is required. The greater pressure implies more gas and still greater friction. This effect sets an approximate upper bound on target speed at the speed for which the frictional force and the force on the projectile become equal. For a 1 cm diameter sabot accelerated at 1000 g's with room temperature hydrogen, this effect limits the sabot speed to about 400 m/s. Since F_{fg} is proportional to u^4 , this limit is not very sensitive to small changes in target size, gas temperature, or acceleration. Above this speed, the pressure requirement at the barrel inlet rises very rapidly. More complex methodologies such as mid-barrel gas injection ports would be required to achieve velocities greater than 400 m/s.

3.3.1.4. Gas Gun Direct Drive Sabot. In the case of indirect drive targets, the hohlraum and hohlraum supporting structure provides adequate insulation of the capsule from the propellant gas. However, a direct drive capsule would have to be isolated from the warm gas by a special sabot. As discussed below, thermal protection in the gun might not be too difficult.

A potential axisymmetric sabot design is illustrated in Fig. 3–7. The fit tolerance of the two halves of the sabot will be set to allow for easy manufacture. The two halves of the sabot do not form a gas tight seal, but do form a long narrow path for gas to travel through. Any gas that makes it into the vicinity of the capsule will be cooled to near the sabot temperature in transit. The sabot heats from the outside. Time is required for the inner temperature to start to rise significantly. One heat transfer relationship from Ref. 3–9 that is useful involves the transient temperature of a slab of thickness $2l$ with a

Fig. 3-7. The sabot isolates the capsule from warm propellant gas during acceleration and separates due spring force after leaving the barrel.

constant initial temperature t_i . At time = 0, the temperature of the surface is changed and held at a new constant temperature t_0 . The temperature of the slab is given by

$$\frac{t - t_0}{t_i - t_0} = 2 \sum_{n=0}^{\infty} \frac{(-1)^n}{\lambda_n} \cos(\lambda_n x) \exp(-\lambda_n^2 Fo)$$

where

$$\lambda_n = (2n + 1)\pi / 2$$

$$Fo = \frac{\alpha \tau}{l^2}$$

α = thermal diffusivity and

τ = time

A 1 mm thickness of a cryogenic plastic with a thermal diffusivity of $10^{-6} \text{ m}^2/\text{s}$ is sufficient to restrict the temperature rise of the surface away from the heat source to less than 1 part in 1000 for 50 ms. The material thickness required is proportional to the square root of the thermal diffusivity times the heating time.

The spring should be pre-compressed during loading. It will be held in compression by the inertia of Part 2 of the sabot (Fig. 3-7) during acceleration. This spring force of this design is limited to the force required to accelerate Part 2 of the sabot. Part 2 should seat on Part 1 during acceleration rather than oscillate on the spring. The capsule will roll back in its cavity during acceleration. The two halves of the sabot will be of roughly equal mass of order 1 g each. The speed at which the spring will force the two halves apart depends on the distance over which the spring acts. For example, suppose a 2 g

sabot is accelerated at 1000 g's to a speed of 400 m/s and the spring is compressed a distance x of 1 cm. The desired spring constant k is given by

$$k = \frac{m_2 a}{x} = \frac{0.001 \text{ kg}(10000 \text{ m/s}^2)}{0.01 \text{ m}} = 1000 \text{ N/m}$$

where m_2 is the mass of part 2 of the sabot. Upon separation the spring energy E given to each half of the sabot is

$$E = \frac{1}{2} \frac{1}{2} k x^2 = 0.5(0.5)(1000 \text{ N/m})(0.01 \text{ m})^2 = 0.025 \text{ J} \quad .$$

The velocity v of each sabot part in the center of mass frame is given by

$$v = \sqrt{\frac{2E}{m}} = \sqrt{\frac{2(0.025 \text{ J})}{0.001 \text{ kg}}} = 7 \text{ m/s} \quad .$$

The time t required for the spring to expand (go through 1/4 cycle of an oscillation) is given by

$$t = \frac{\pi}{2} \sqrt{\frac{m_r}{k}} = \frac{3.14}{2} \sqrt{\frac{0.0005 \text{ kg}}{1000 \text{ N/m}}} = 1.1 \text{ ms}$$

where m_r is the reduced mass of the system.

The target is protected from external gas heating during this time in which the target travels 0.44 m. The external gas pressure must be reduced as much as possible in this distance (probably requires <1 Torr) and continue to be reduced further during the coasting distance over which tracking occurs. Baffle chambers with shutters and differential pumping can be used to reduce the gas pressure. Part 1 of the sabot will tend to reroute gas that is streaming directly from the gun barrel away from the capsule. We could reasonably place two or three baffle chambers prior to the sabot separation point. The spring constant can be reduced if we desire to extend the sabot opening distance. After the sabot is clear of the center of the capsule, target tracking can begin. The target tracking will be complicated by the sabot, but this should not present a major obstacle. After the sabot is well clear of the capsule, it may be deflected away from the reaction chamber opening. As illustrated in Fig. 3-8, a spinning wheel with speed matched to the target speed would allow deflection of the sabot without slowing the sabot so much that the capsule runs into the sabot.

We have not yet tested the accuracy that can be achieved with this type of sabot and a cryogenic IFE target. However, the tests using this type of plastic ball support and the LBNL gas gun with a pulsed magnet give reason for optimism. As was done at LBNL, the sabot and barrel would likely need to be rifled to prevent excessive sabot tumble.

Fig. 3-8. A rotating wheel deflects the sabot toward a recycling catcher without slowing the sabot. The capsule's path is not affected.

3.3.1.5. Conclusions. A gas gun is a simple target injection device. Friction and low acceleration will probably limit target injection speed to roughly 400 m/s. A simple reservoir and valve system should suffice for relatively low injection speeds. A twin reservoir system with throttled pressure increase should allow reaching the 400 m/s target speed requirement. A spring loaded sabot should enable the use of a gas gun for direct drive target injection.

3.3.2. Electrostatic Accelerator

As illustrated in Fig. 3-9, an electrically charged target is suspended and accelerated in an electric field. Oscillating quadrupole fields have successfully levitated 0.8 mm diameter spheres for days as described in Ref. 3-10. These spheres were transported at speeds up to 0.1 m/s in air.

The force that can be electrostatically applied to a target is severely limited by vacuum sparking (field emission). The maximum cathode field is approximately given by Kilpatrick's Criterion

$$VE^2 \exp\left(\frac{-1.7 \times 10^7}{E}\right) \leq 1.8 \times 10^{18}$$

where V is the potential difference in volts and E is the cathode electric field in volts/m. This limits the acceleration of a negatively charged direct drive target to about 100 g's. The acceleration of a positively charged direct drive target could be a few times larger. Larger mass prohibits the use of electrostatic acceleration with indirect drive targets. The non-contacting nature of electrostatic acceleration has the significant advantages of not

destroying, scratching, or conducting heat to a direct-drive target, thereby removing the need for a sabot. However, control of target radial position becomes more of a challenge without physical contact. Reference 3-1 provides more information on this injection method.

Fig. 3-9. The target is dynamically confined between quadrupole rails. Alternatively, active feedback electric field control could be used to control target position and velocity. A dc field is applied to counteract gravity. Accelerating electrodes provide a traveling electric field to accelerate the target (Ref. 3-10).

3.3.3. Magnetic Accelerator

3.3.3.1. Rail Gun. The rail gun is the simplest magnetic accelerator. As illustrated in Fig. 3-10, current flows through one rail through (or behind) the target, and back through the opposite rail. The magnetic field due to the current flowing through the rails interacts with the current passing through the target to produce a force that propels the target. The roughly 2.2 kA current required to give a 1 g target $2,000 \text{ m/s}^2$ acceleration can be reduced to about 260 A by applying a 1 T external magnetic field. Maintaining good electrical contact with the target while minimizing wear is an issue for a rail gun. (Reference 3-1 provides more information on this injection method, but incorrectly calculated the currents in the above example.)

Fig. 3-10. Schematic of solid armature rail gun for target injection. The cylindrical shape of a typical rail gun bore is shown (other shapes may be used).

3.3.3.2. Induction Accelerator. An induction accelerator is more complicated than a rail gun but has the significant advantage of not requiring electrical or physical contact with the target. The barrel consists of multiple conducting coils as illustrated in Fig. 3-11. Each coil is connected to a separate phase of a multiphase power supply thereby producing a traveling magnetic wave with a radial component that travels faster than the projectile. The concept requires a conducting ring in the projectile in which a current is induced by the traveling magnetic field. A similar system is also described in Appendix L. We propose additional study of this method as a back up to gas gun injection.

Reference 3-1 discusses how to calculate the force on such a projectile. Adequate acceleration of an indirect drive target with a conducting sleeve to speeds of ~400 m/s should be possible without excessive sleeve heating or barrel heating.

Fig. 3-11. A six-phase induction accelerator.

3.3.3.3. Iron Insert (Synchronous) Accelerator. The barrel of the iron insert accelerator is very similar to the induction accelerator. As illustrated in Fig. 3-12, a ferromagnetic cylinder is placed into the sabot. This cylinder is attracted toward the energized coils ahead of it. The coils must be de-energized as the target passes. The magnetic force is unstable to radial motion, so this accelerator requires physical contact between the barrel and the sabot to keep the sabot centered. It is a relatively simple accelerator that appears viable for low speed injection. The saturation magnetic field limits this acceleration method to about 1,000 g's. This accelerator is described more fully in Appendix M.

Fig. 3-12. The ferromagnetic (iron) cylinder is attracted towards the energized coils in front of it.

3.3.3.4. Permanent Magnet Insert Accelerator. In this approach the iron insert is replaced by a permanent magnet which might allow a greater dipole moment and therefore larger acceleration with the same field current. As discussed in Appendix M, the concern is that the magnetic insert might become demagnetized by the coil field, especially if a repulsive field is applied.

3.3.3.5. Super Conducting Insert Accelerator. A hollow super-conducting insert could be used to replace the previously discussed magnetic insert. In this case the force is repulsive so the coils behind the insert would be energized as the insert passes. There is also a centering force so that it might be possible to operate this accelerator in a non-contacting mode. Further evaluation is necessary to determine the feasibility of non-contacting operation. Several high T_c superconductors operate well at target injection temperatures, but the maximum current in presently available super conducting materials would probably limit acceleration to a few hundred g's. Higher current superconductors are expected to be available in the next few years. This method is discussed further in Appendix M.

3.4. Target Injection System Selection

Previously proposed injection and tracking methodologies have been reviewed, and additional technologies have been evaluated. Light gas guns, electrostatic accelerators, and a variety of magnetic accelerator options were considered. The light gas gun appears to be the simplest and lowest cost option (and initial experimental work has already been conducted), but is limited to about 400 m/s for the design requirements of this application. Electrostatic accelerators could be suitable for the low mass of direct drive targets but are limited to acceleration of about 100 g's (and consequent long injection system lengths). Table 3-2 provides a checklist that shows how four candidate target injectors measure up to our primary target injection requirements. Barrel swelling due to neutron damage, target chamber products in the barrel, barrel wear, gas injection into the target chamber, and cycle time were also factored into the evaluations. Only the gas gun and induction accelerator meet all listed requirements. However, while the gas gun is a demonstrated technology, the induction accelerator would require a significant development effort.

The light gas gun is recommended for the experimental demonstration program. This technology is expected to meet all requirements, both for the demonstration program and for an IFE power plant. The relative simplicity and cost of the gas gun technology allows construction of a full-length injection and tracking system to demonstrate both direct and indirect drive target injection during Phase I. This full-length injector will be an effective prototype of the systems needed for the Integrated Research Experiments during Phase II.

The gas gun has the disadvantage that the propellant gas may interfere with target steering. In the event that the gas from the gas gun results in unforeseen and unsolvable difficulties in actual use, it is also recommended that the magnetic induction system be

evaluated as a backup. This includes construction of a short proof-of-principal system with a metallic conductor in Phase I to verify the design concept.

Table 3-2
Target Injection Requirements Checklist

Requirement/ Parameter	Experimental Injector Requirements	Gas Gun	Iron Insert	Induction Accelerator	Electrostatic Accelerator
1. Target speed (m/s)	400 for direct drive 180 for indirect drive	Yes Yes	Yes (8m) Yes	Yes Yes	No No
2. Acceleration (g's)	5000	Yes	No	Yes	No
3. Repetition rate (Hz)	Multi-shot "burst" capability, upgradable to cryogenic	Yes Yes	Yes Yes	Yes Yes	Yes Yes
4. Target mass (g)	0.005 (direct drive) 1 (Indirect drive)	Yes Yes	Yes Yes	Yes Yes	Yes No
5. Target diameter (mm)	5 (direct drive) 12 (indirect drive)	Yes Yes	Yes Yes	Yes Yes	Yes NA
6. Target temperature range (K)	18-19 (initial injections at room temperature)	Yes Yes	Yes Yes	Yes Yes	Yes Yes
7. Target spin (rotations per second)	300	Yes	Yes	Yes*	NA
8. Target exit accuracy (mradians)	±0.3	Yes	Yes	Yes	Yes†
9. Cryogenic fuel temperature rise (from start of injection to center of chamber) K	0.5	Yes	Yes	Yes	Yes
10. Availability (%)	Several "bursts" per hour for experiments	Yes	Yes	Yes	Yes

* May require contacting system.

† Requires development of active positional control.

3.5. References

- 3-1 R.W. Petzoldt, Inertial Fusion Energy Target Injection, Tracking, and Beam Pointing, Lawrence Livermore National Laboratory Report UCRL-LR-120192, (1995).
- 3-2 J. Sater, et al., "Progress on Beta Layering in Spheres," October 20, 1998, Private Communication — Unpublished Data.
- 3-3 D.D. Ho, et al., "Radiation-Driven Targets for Heavy Ion Fusion," Presented at the Fifteenth International Conf. on Plasma Physics and Controlled Nuclear Fusion Research, Seville, Spain, 1994, (International Atomic Energy Agency, Vienna, 1995) IAEA-CN-60/B-P-13.
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- 3-5 D.G. Miller, "Ballistic Tables for Spheres 7.5 to 25 mm (0.3 to 1 in.) in Diameter," Lawrence Livermore National Laboratory Report UCRL-52755, 1979.
- 3-6 Handbook of Chemistry and Physics, 52nd edition (1971).
- 3-7 R.W. Petzoldt, "IFE Target Injection and Tracking Experiment," Fusion Technology **34**, 831 (1998).
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- 3-9 J.H. VanSant, "Conduction Heat Transfer Solutions," Lawrence Livermore National Laboratory Report UCRL-52893, Rev. 1 (1983).
- 3-10 W.L. Johnson and C.D. Hendricks, "Electrostatic Levitation and Transport of Laser Fusion Targets," Lawrence Livermore National Laboratory Report UCRL-83981 (1980).

4. Selection of a Tracking System

4.1. Tracking Systems Considerations

We must know the target's position in order to precisely hit it with driver beams. We could potentially track the target continuously, but it is easier to measure the target's position at a few points and predict subsequent motion. If the target trajectory will not lead it with sufficient precision into the beam path, we must either steer the target or steer the beams. We also use the target position information for driver beam timing. Target tracking occurs in a high radiation environment and sensor shielding will be required.

4.2. Tracking System Requirements

Target tracking must be accurate enough to enable precise alignment of the driver beams and the targets. Current alignment accuracy requirements for indirect-drive targets are ± 0.2 mm perpendicular to the injection axis, and about ± 1 mm along the injection axis. The relaxed requirement along the injection axis is due to the narrow angle from which the beams approach along this axis. Direct-drive targets will require even greater alignment accuracy of ± 0.02 mm. In either case, failure to meet these alignment accuracy requirements will lead to reduced target gain. Target position prediction must be accomplished early enough to allow time to take action such as beam steering or target steering.

4.3. Tracking Methods

Two potential tracking methods are optical and capacitive. Optical tracking could be achieved by continuously viewing an illuminated target from a sensor located along the injection axis. However, it is considerably easier to set up tracking stations with light sources and photodiodes which measure the target's position perpendicular to the motion as shown in Fig. 4-1. These sensors also measure the time that the target passes their known locations. A capacitive detector consists of electrodes with voltage sources through which the target passes as shown in Fig. 4-2. The target will affect the system capacitance. The current flow and net charge on each plate should be correlated with the target position as it passes through the detector. The ammeters shown would likely be replaced with resistors and peak voltage measuring circuits.

Fig. 4-1. Target position detector. The light source is collimated and the horizontal and vertical measurements are independent.

Fig. 4-2. A capacitive detector may be used to sense the target's position.

4.4. Beam Steering

4.4.1. Laser Beam Steering

Based on target position predictions, the laser beams must be directed to hit a direct-drive target with $\pm 20 \mu\text{m}$ accuracy. Appendix J shows that commercially available mirror mounts can accelerate and move quickly enough to point laser beams for tracking signals. The quoted accuracy of the mirrors does not meet the $20 \mu\text{m}$ direct drive requirement, but the quoted resolution does. The mirror accuracy might be improved if their range is reduced from 360° to about 70 arcs that would be required for beam pointing. Appendix J also suggests using linear motors to position a $1 \text{ m} \times 4 \text{ m} \times 1 \text{ cm}$ grazing incidence metal mirror. The resolution and force available with linear motors is shown to be sufficient to position such a mirror in the time available. Both methods assumed that the pointing mirror would be the final optic located 30 m away from the target. It might be more effective to redirect the beams earlier in the beam lines where the beams are smaller, less energetic, and out of the line of sight of the fusion neutrons. This option is recommended for further study.

In any case, evaluations indicate the beam steering requirements for direct drive can be met with systems using currently available technology. However, maintaining positional stability of the beam steering apparatus will be critical.

4.4.2. Ion Beam Steering

Reference 4-1 discusses ion beam steering for 10 GeV 200 amu singly charged ion beams. A 4 GeV beam will take less field to steer because it is only moving $6 \times 10^7 \text{ m/s}$ rather than 10^8 m/s . Assuming a 0.25 m long steering magnet and a 5 m magnet standoff distance to steer a beam 0.01 m, the required field is

$$B \approx \frac{y_c v_x m}{x_c x_a q} \approx \frac{0.01 \text{ m} (6 \times 10^7 \text{ m/s}) (200 \text{ amu}) \left(\frac{1 \text{ kg}}{6 \times 10^{26} \text{ amu}} \right)}{0.25 \text{ m} (5 \text{ m}) (1.6 \times 10^{-19} \text{ Coul})} = 1 \text{ T.}$$

rather than 1.65 T required for a 10 GeV beam.

The field energy required is directly proportional to the beam energy and proportional to the steering distance squared. The power used in steering large numbers of driver beams large distances can be significant. It might be possible and more efficient to make steering corrections farther up the beam line. For example, a small steering correction could place the beam off center in a focusing quadrupole. The quadrupole field could then steer the beam to the desired location.

The dispersion of the beam spot caused by a uniform steering field is proportional to the beam momentum dispersion. So a $\pm 1\%$ beam dispersion leads to a $\pm 0.1 \text{ mm}$ beam focus

dispersion when steering a beam 10 mm. Of course, this also requires the steering field integrated along the beam path to be uniform to better than 1%.

4.5. Tracking System Selection

Optical detectors such as shown in Fig. 4-1 were used with good success to measure indirect drive target position at LBNL (Ref. 4-2). However, these detectors must be kept clean of films that would inhibit light and be well shielded from radiation. A capacitive detector should be less sensitive to radiation and thin films. We recommend use of the optical detectors with further investigation of and possible testing of capacitive detectors. Lasers will probably be used to replace the light emitting diodes. This will allow greater standoff of the light source and detectors from the target path, facilitating shielding and cleanliness. The greater standoff allows the possibility of in chamber target tracking through small holes in the reaction chamber wall. In-chamber tracking may required, especially for low-mass direct-drive targets passing through a gas filled chamber.

4.6. References

- 4-1 R.W. Petzoldt, Inertial Fusion Energy Target Injection, Tracking, and Beam Pointing, Lawrence Livermore National Laboratory Report UCRL-LR-120192, (1995).
- 4-2 R.W. Petzoldt, "IFE Target Injection and Tracking Experiment," Fusion Technology 34, 831 (1998).

5. Proposed Test Program

The proposed testing program for target injection and tracking is focused on providing demonstration data addressing the key feasibility issues of:

- Ability of targets to survive the chamber environment (target heating due to radiation and chamber gases)
- Accuracy and repeatability of target injection and tracking (ability to provide suitable beam steering and/or target steering, and shot-timing signals)
- Ability of targets to withstand acceleration into the chamber (strength of target components, including the DT itself).

Figure 5-1 shows the logic of this test program in an overview fashion. The ultimate goal of this development program is to provide a demonstration of injecting prototypical IFE cryogenic targets into a hot chamber that is representative of an operating target chamber. This demonstration will take place in Phase II of the IFE development program, as practical target designs are developed and after the injection and cryogenic target filling and layering equipment is ready. In Phase I, the technical approach comprises gathering of data on the thermal response of DT-filled targets via a "stationary cryostat", demonstrating injection accuracy and tracking at room temperature, and providing relevant DT strength data to allow modeling of cryogenic performance. These initial data and analyses will allow definition of a practical target design which can be tested in

Fig. 5-1. Logic diagram for IFE target injection and tracking development program

Phase II in the experimental injection system. Defining the ultimate target design is an iterative process that must take place throughout Phase I — and continue into Phase II. It must account for target design requirements, target chamber protection schemes, environmental and safety considerations, target injection parameters, and overall plant system considerations.

The test program is structured to allow a logical progression of activities from initial data acquisition to proof-of-principle demonstration tests. The Phase I experimental program provides for a logical follow-on to Phase II cryogenic injection demonstrations. The experience with this initial demonstration system will be utilized to develop high-repetition injection systems to support IRE operations later in Phase II.

This chapter is divided into sections which correspond to the WBS breakdown of Task 3 of the IFE Technology Element of the OFES-VLT. Subtasks corresponding to each WBS element are discussed below.

5.1. Target Thermal Response (WBS 3.2.1)

5.1.1. Data Needed

Targets are heated during injection by exposure to the thermal radiation from the target chamber and by exposure to gases in the target chamber. For example, the reference SOMBRERO target chamber (direct drive targets) is at a temperature of approximately 1750 K and includes 0.5 Torr of xenon gas to protect the first wall from x-rays and debris. A key feasibility issue for IFE is the ability of the cryogenic targets to survive their transit to the target chamber center and remain in a condition suitable for high gain energy production. Data are needed demonstrating the capability of the target to survive this thermal environment.

5.1.2. Key Conditions

Direct Drive (Based on SOMBRERO IFE Power Plant Concept)

Reference Parameter	Value
Target chamber first wall temperature	~1750 K
Target chamber gas pressure	$\sim 2 \times 10^{16} \text{ cm}^{-3}$ 0.5 Torr (pressure at standard temperature)
Target chamber gas temperature	1758 K
Target chamber radius at midplane	~6.5 meters
Target configuration	Capsule with internal DT (no hohlraum)
Temperature of DT in target	18–19 K
DT ΔT limit	~0.5 K (preliminary value)

Indirect Drive (Based on HYLIFE-II IFE Power Plant Concept)

Reference Parameter	Value
Target chamber first wall temperature	~900 K
Target chamber gas pressure	$\sim 3 \times 10^{13} \text{ cm}^{-3}$ $\sim 10^{-4} \text{ Torr}$ (pressure at standard temperature)
Target chamber gas temperature	~900 K
Target chamber radius at midplane	~3.5 meters
Target configuration	DT-filled capsule in hohlraum
Temperature of DT in target	18–19 K
DT ΔT limit	~0.5 K (preliminary value)

5.1.3. Approach

The objective of this testing in Phase I is to determine the effect on the DT layer of a heat flux imposed on the capsule.* The magnitude of the surface heat flux during actual injection into a target chamber (from gas convection and/or thermal radiation) will be estimated in Phase I with calculational codes. Existing ICF equipment located at LANL will be utilized to provide initial thermal response data in Phase I. The Cryogenic Pressure Loader (CPL) is designed to permeation fill with DT, beta-layer, and examine direct drive ICF capsules. This equipment will be modified to provide for a brief exposure to thermal radiation and provide a heat flux to the IFE target. The response of the layered fuel to this heat flux can then be observed. Methods for thermal protection of the target (e.g., reflective films) will be developed, in conjunction with target fabrication personnel, and demonstrated in the apparatus.

The results of the Phase I measurements and analyses will be confirmed in Phase II by incorporating cryogenic target fill and layering equipment into the demonstration injector system to provide an integrated demonstration of survivability of injected targets.

5.1.4. Test Apparatus

Figure 5–2 is a conceptual diagram of the proposed experimental apparatus. The target is suspended in a “Stationary Cryostat”. Diagnostic systems are used to characterize the fuel layer thickness and smoothness. A source of infrared radiation that is tunable to provide radiation with the desired spectrum and intensity is used to impart the heat flux. The source is energized and shutters opened for a short period of time which represents the time that a target is in the reaction chamber of a power plant.

* As opposed to attempting to simulate directly all characteristics of the environment seen by the target during injection. Such a simulation is a more complex task that will be done during Phase II.

Fig. 5-2. Conceptual view of exposing targets to infrared radiation to determine the effect on the DT layer.

Currently, visible light interferometry is used to determine fuel layer thickness and surface smoothness (through transparent capsules). It would be desirable to do real time diagnostics, taking multiple snapshots of the data so that a progression of target and fuel degradation could be observed as a function of time. The current strategy for protection of the direct drive target from thermal radiation is to provide an outer surface that is highly reflective to infrared radiation. However, to be able to characterize changes in the DT layer due to heating one must have a coating that is transparent to the wavelengths used for the characterization. This might require a coating that is reflective to infrared wavelengths and transparent to visible or ultraviolet wavelengths. This would be a desirable property not only for this particular test but for development and quality control of targets in a production environment. A portion of this work (jointly with target fabrication tasks) will evaluate the development of infrared reflective films on the surface of the direct drive capsule. Possibilities for such coatings, along with some initial measurements of IRE reflectivity of multi-layered gold/Kapton coatings, are discussed in Appendix K.

5.1.5. Work Breakdown, Costs, and Schedule

WBS	Task	Operating Costs \$K			
		FY00	FY01	FY02	FY03
3.2.1.1	Component Design, CPL Modification and Setup	150			
3.2.1.2	Acquisition of DT Performance Data and Reflective Film Development		200	150	150
3.2.1.3	Data Correlation and Documentation			50	50
	Capital Costs	50			
	Total Costs	200	200	200	200

Due to the necessity to work with tritium and the availability of suitable existing equipment, it is anticipated that the workscope described above will be conducted at LANL.

5.2. Target Injection Accuracy and Tracking (WBS 3.2.2)

5.2.1. Data Needed

Injection accuracy and tracking requirements are to place a target to within ± 5 mm (for both indirect and direct drive), and to track the target and provide signals to align the driver beams to within ± 20 μ m for direct drive and to within ± 200 μ m for indirect drive. Injections with projectiles simulating targets at room temperature have already been demonstrated at up to ~ 70 m/s with an accuracy for projectiles simulating indirect drive targets of ± 220 μ m when extrapolated to power plant conditions.

Data are needed to demonstrate meeting both the direct and indirect drive injection and tracking accuracy requirements at the expected velocities of up to 180 m/s for indirect drive and up to 400 m/s for direct drive targets. This demonstration shall consist of injecting room temperature projectiles, tracking their progress, predicting their position, and then measuring their final position and comparing it to the predicted position.

5.2.2. Approach

Issues of accuracy and repeatability will be addressed with experimental demonstrations. A new injector will be constructed and utilized to demonstrate higher velocity direct drive and indirect drive injection and to measure the accuracy of placement. The injector will be utilized to measure accuracy with room temperature (simulated) targets during Phase I. It will be designed for later (Phase II) interfacing with cryogenic permeation fill and

transport systems for a full, cryogenic target injection demonstration into a high-temperature surrogate chamber.

5.2.3. Test Apparatus

Figure 5-3 illustrates a conceptual overview of the entire test setup to demonstrate cryogenic target injection accuracy and tracking. This layout includes the cryogenic systems that are to be added in Phase II to provide filled and layered targets for follow-on demonstrations.

Fig. 5-3. Conceptual layout of experimental injector system (includes cryogenic components planned for Phase II).

Figure 5-4 (foldout) provides a more detailed illustration of the equipment to be constructed in Phase I. The injector is a light gas-gun system that meets all of the requirements stated in Table 3-1. It is capable of achieving a velocity of up to 400 m/s for direct drive target injection, and accomplish this at accelerations of 1000 g's. The system is a full-length prototype of that proposed for use with the IRE. Components necessary for demonstrating high-repetition rate room-temperature injection and tracking of both direct and indirect drive targets are included. These components include a custom precision rifled barrel, a multiple projectile feeder system, tracking lasers with precision mounting and tracking equipment, a real-time computer control and data acquisition system with software, a high speed camera system, a computer controlled pressure-ramping gas supply system, vacuum system with differential pumping and systems to handle the injection gas, and vacuum vessels., and a furnace for high-temperature target injections. The entire system will be capable of a one-second "burst" at 6 Hz. Cryogenic equipment for filling, layering, and supplying cryogenic targets, as well as the high-temperature furnace systems, are planned for Phase II.

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AND TRACKING DEMONSTRATION

Fig. 5-4. Layout of proposed Phase I injection equipment.

For initial accuracy and tracking demonstration purposes, the room temperature targets will be loaded into a light gas gun via a feeding system. Projectiles simulating direct drive targets will be enclosed in a sabot. Indirect drive target projectiles may have additional components for injection as well, if found necessary. After injection the sabot will be removed (for direct drive) and the target's flight will be tracked

A final position detection system will be utilized during testing to determine the accuracy of target placement and the ability to predict the precise position of the target. Evaluations of target steering as a more efficient means of centering the target with the driver beams are also planned.

5.2.4. Work Breakdown, Costs, and Schedule

The costs below are grouped together for WBS 3.2.2 (Target Injection Accuracy and Tracking) and WBS 3.2.3 (Target Acceleration Response) since both of these tasks rely on design, construction, and operation of a new injector system in Phase I. The injector will provide data on accuracy and tracking (at room temperature) during Phase I and, during Phase II, will provide data on the acceleration response of cryogenic targets.

Costs to support initiation of the design of systems for providing filled and layered targets are included in the last year of the program, as well as costs to document the testing and demonstration results for the Phase II (IRE) decisions. The lines in the table below indicate the time frame of the workscope. A further breakdown of the manhours and capital cost estimates for this task are provided in Appendix N.

WBS	Task	Operating Costs \$K			
		FY00	FY01	FY02	FY03
3.2.2.1 3.2.3.1	Injection System & Supporting Equipment Design	1000			
3.2.2.2 3.2.3.2	Procurement & Fabrication	150	400		
3.2.2.3 3.2.3.3	Assembly and Initial Shakedown		550	400	
3.2.2.4 3.2.3.4	Acquire Data, Modify Equipment to Optimize Results			750	600
3.2.2.5 3.2.3.5	Doc. Results/Support IRE Decision				150
3.2.2.6 3.2.3.6	Initiate Design of Cryogenic Equipment				250
3.2.2.7 3.2.3.7	Evaluate Magnetic Injector System Backup		50	150	300
	Capital Costs	150	250		
	Total Costs	1300	1250	1300	1300

5.3. Target Acceleration Response (WBS 3.2.3)

5.3.1. Data Needed

Extrapolations of existing strength data for hydrogen isotopes indicate that acceleration forces of up to about 1000 g's can be withstood by the DT ice layer during injection. However, this is based on data from deuterium at 16.4 K and data are needed for DT at the injection temperature (approximately 18 K). These temperatures are approaching the melting point where properties are changing rapidly and the DT strength may be a function of time due to He-3 buildup.

The absolute minimum acceleration that a target must be able to withstand is unknown at this time, as final target designs are not complete nor have trade-offs on injector length, target heating, injection velocity, and acceleration requirements been done. Data on the acceleration capabilities of DT are needed for target and injector system design and overall system trade studies. While DT ice properties for direct drive targets (with their higher injection velocity and acceleration forces) are currently the primary focus of this task, other target components such as hohlraum materials and structures may require additional data, depending on the evolution of the target designs and materials.

5.3.2. Approach

The technical approach to determining the target acceleration response is divided into two stages, coinciding with Phase I and Phase II of the overall program. The first stage consists of gathering fundamental material property data for analytical evaluations of target stress during injection. These analytical evaluations will form an integral part of an iterative target design process, which will take into account a multitude of material property and plant performance considerations. The second stage consists of experimentally demonstrating the capability of the target to withstand the required accelerations under representative injection conditions. This demonstration will be done with filled and layered cryogenic targets in the experimental injector system during Phase II.

5.3.3. Test Apparatus

Gathering of fundamental property data with regard to the target acceleration response (Phase I work) is described in Section 5.4. The design and construction of an injection and tracking demonstration system for integrated testing (injections with cryogenic targets in Phase II) is described in Section 5.3.

5.3.4. Work Breakdown, Costs, and Schedule

Costs in Phase I for addressing the target acceleration response are covered under WBS 3.2.4 (Target Property Measurements). Additional Phase I costs attributed to target acceleration response (WBS 3.2.3) are based on a partitioning of the costs for design and construction of the experimental injection system. These costs are grouped with those given above for WBS 3.2.2 (Target Injection Accuracy and Tracking).

5.4. Target Material Properties (WBS 3.2.4)

5.4.1. Data Needed

Measurements of DT yield strength to allow more accurate calculation of the acceleration response of targets are needed. Additional materials property data for target components may be needed as specialized materials are developed for use in targets. These include such items as low-density hydrocarbon and metallic foams. While these materials are predominantly utilized in indirect drive target designs, with a much lower injection velocity and acceleration, their strength during injection may be an issue.

Recent work by LLNL on the ICF program indicates that a layered target can only sustain a temperature change of ~ 0.5 K in the DT without increasing the ice layer roughness enough to significantly degrade target performance. This is an issue for ICF since the smoothest surface finish can be obtained by layering just below the DT triple point (~ 19.7 K) but planned shot temperatures are in the vicinity of ~ 18.5 K. This is a further issue for IFE since the targets must be cooled to an even lower temperature (e.g., ~ 18 K) after layering to allow some margin for heatup during the injection process. While the ICF program is addressing cooldown of targets (to ~ 18.5 K) after layering, it may be necessary to extend the ICF layering studies to the conditions needed for IFE injection.

5.4.2. Approach

The technical approach chosen is direct measurements of the tensile yield strength of DT ice. This work is planned to take place in tritium facilities at LANL.

If additional studies of target surface finish as a function of cooldown after layering are needed, these will be done as an extension of ICF research programs.

5.4.3. Test Apparatus

An apparatus for measuring the stress-strain curves for hydrogen isotopes is shown in Figure 5-5 (adapted from Ref. 5-1). Liquid hydrogen is condensed in the innermost ampoule, then cooled to the solid state. The heaters are operated momentarily while

Fig. 5-5. Conceptual layout of test apparatus for measuring the tensile strength of solid DT.

evacuating the hydrogen vapor to detach the sample from the ampoule walls. This leaves a sample of approximately 30 mm length and 6 mm diameter attached to the ampoule at the bottom and to a moveable quartz rod at the top. The temperature can be regulated by controlling power to the heater and by controlling the gas pressure in the surrounding jacket (thus controlling the conductance to the liquid helium). A similar arrangement can be envisioned with cooling power supplied by a mechanical cryocooler. Temperature sensors can actually be embedded in the sample. Additional instrumentation includes a load sensor and position sensor.

5.4.4. Work Breakdown, Costs, and Schedule

WBS	Task	Operating Costs (\$K)			
		FY00	FY01	FY02	FY03
3.2.2.1 3.2.3.1	Experiment and Equipment Design		130		
3.2.2.2 3.2.3.2	Measurement of DT Strength/Target Material Properties			300	300
	Capital Costs		20		
	Total Costs		150	300	300

5.5. Reference

- 5-1 I.N. Krupskii, A.V. Leont'eva, L.A. Indan, and O.V. Evdokimova, "Plastic Deformation of Solid Hydrogen," Soviet J. Low Temp. Phys. **3**, 453 (1977).

6. Summary of Accomplishments and Recommendations

The FY99 accomplishments focused on several major areas (a) target and equipment evaluations necessary to define injection and tracking system requirements, (b) evaluations of potential injection and tracking system technologies that could meet these requirements, (c) selection of injection and tracking equipment for the experimental program (d) definition of Phase I data needs and experiments to be conducted to address the key feasibility issues of injection and tracking, and (e) meeting with target and plant designers to integrate the efforts towards practical, optimized IFE targets.

6.1. FY99 Accomplishments

1. Estimates of the effects of target chamber gas on target trajectories and target heating.
2. Calculations of the surface heat flux to the target from thermal radiation and exposure to chamber gases during injection.
3. Using a gas gun for injection, demonstration of sabot removal from a plastic capsule at room temperature with a pulsed magnet and without significant accuracy degradation (with equipment at LBNL).
4. Thermal radiation reflectivity measurements over wavelengths applicable to direct drive target chambers of gold/Kapton foils similar to those proposed for the surface of IFE direct drive targets.
5. Finite element code calculations of DT temperature distributions as a function of surface heat flux for currently proposed target designs, both direct and indirect drive.
6. Evaluations of the capabilities of a wide variety of injector and accelerator technologies, ranging from simple gas guns to advanced high-temperature superconductor systems that should become available in the next few years.
7. Specific calculations to evaluate the use of light gas guns for target injection at the low accelerations needed for IFE targets for both direct and indirect drive targets.
8. A preliminary design and thermal analysis of a gas gun sabot to protect a direct drive target from the warm propellant gases during injection.
9. Evaluations of target detection and position prediction equipment for use during injection.

10. Evaluation of the effects on indirect drive target trajectory from Flibe droplets in the injection path.
11. Preliminary feasibility calculations for using shutters and a gas counterflow to keep Flibe vapor out of the target injection area in an IFE power plant.
12. Organized the Heavy Ion Fusion Target Workshop and the Direct Drive Target Workshop to assist in coordinating efforts between groups, and to move towards practical IFE targets.
13. Participated in and made presentations at the IFE Chamber and Target Technology Meeting (March 1999) and the Snowmass Fusion Summer Study (July 1999).
14. Further development of preliminary injection and tracking system requirements for a power plant injection system, and development of requirements for the experimental injection and tracking system.
15. Development of an injection and tracking system testing and demonstration program strategy and approach.
16. Selection of recommended injection and tracking system technologies to be employed for the Phase I testing and demonstration program.
17. Recommendation of injection and tracking supporting tasks, materials data needs, and specific tests to provide that data in Phase I.
18. Preliminary recommendations of follow-on tasks to be accomplished in Phase II.

6.2. Summary and Recommendations

Key issues associated with target injection and tracking for Inertial Fusion Energy have been evaluated. Previously proposed injection and tracking methodologies for IFE target injection and tracking have been reviewed, along with additional technologies that have become available in recent years. A four-year experimental plan was developed to provide data to address the key issues. A series of experiments is recommended including material property measurements, measurements of the thermal environment effects on filled targets, and demonstration of the accuracy of target injection and tracking.

Requirements for an injector demonstration system were developed and various technologies were evaluated against these requirements. The light gas gun is recommended for the experimental demonstration program. This technology is expected to meet all requirements, both for the demonstration program and for an IFE power plant. The relative simplicity and cost of the gas gun technology allows construction of a full-length injection and tracking system to demonstrate both direct and indirect drive target injection during Phase I. This full-length injector will be an effective prototype of the systems needed for the Integrated Research Experiments during Phase II.

The test program calls for room temperature operation of the injection demonstration system during Phase I. Cryogenic target filling and layering capabilities will be added to the system during Phase II to allow a full demonstration of cryogenic target survival upon injection into a high temperature surrogate chamber. Continued evaluation of a backup injector utilizing magnetic induction technology is recommended in the event unforeseen difficulties arise with the gas gun injection method.

Appendix A:

Target Injection and Tracking Experimental Equipment Layout, Design, and Operation

Note: This appendix was originally documented as HIFAR Note 485 at Lawrence Berkeley National Laboratory. The appendices to this appendix are archived there.

A.1. Introduction

This is intended to be an informal note for archival purposes that may be of help in restarting the experiment one day. As discussed in Ref. A-1, the target injection and tracking experiment successfully demonstrated real time target sized projectile tracking and position prediction. We subsequently improved the longitudinal position prediction accuracy from 370 microns to 160 microns by rounding the leading edge of the targets with a 2.5 in. radius. We had also intended to combine this experiment with the scaled final focus experiment to demonstrate accurate hitting of projectiles on the fly. Some of the equipment designed to that end will also be described.

A.2. Experimental Layout and Method

The experiment involves using a gas gun to propel cylindrical targets into a vacuum chamber, sense their position with photodiodes, and predict their subsequent position real time with computer interface. A schematic drawing of the experimental layout is shown in Fig. A-1.

The target injector is a helium gas gun. Because of its low molecular weight, helium gas at room temperature has a sound speed which is much greater than the nominal 100 m/s target injection speed. Therefore, helium is a better propellant gas than heavier gases. Since helium is an inert gas, an experiment using helium as a propellant is not subject to the explosive hazard associated with the use of hydrogen gas. Another advantage of helium gas is that it will not freeze on cryogenic targets that will be used in power plant applications. Even though argon has a higher atomic mass than helium it also works well in our gas gun with about a 10% reduction in target speed.

The gas reservoir must have a volume much greater than the volume of the gun barrel so that the gas pressure will not be significantly depleted while propelling several targets out of the gun. The gas flow is controlled with a fast acting solenoid valve. The gun is reloaded with the use of a revolver and stepper motor.

Fig. A-1. Schematic drawing of the experimental layout. Major components from right to left include the gas reservoir with pressure gauge, the gas valve (in front of pressure gauge), the small vacuum tank which encloses the revolver loading mechanism, the gun barrel, the vacuum piping for the target to drift through which holds the first two detectors, the vacuum pump, and the large vacuum tank which holds the final detector and the target catcher.

In the experiment, the revolver is reloaded manually, long after several shots are fired. However, as shown in Fig. A-2, the reloading could be continuous in the power plant application. One target could be loaded into the revolver while another is being accelerated down the gun barrel.

Once the target leaves the gun barrel, target tracking begins. As shown in Fig. A-1, the targets pass through three sets of detectors, located about 10 cm past the end of the gun barrel, 1 m away, and 3 m away. The target's position is detected with photodiodes. As the target passes between the light source and the photodiode, the photodiode is placed in shadow, thereby reducing the electrical output and detecting the target's presence.

Fig. A-2. Target reloading mechanism for gas gun.

The size of our photodiodes is large compared to the required position measurement accuracy along the direction of motion of the target. We improve the measurement precision of this method by use a comparator circuit that switches off when the photodiode amplifier voltage output drops below a preset reference value. This concept is illustrated in Fig. A-3. This is for longitudinal position timing. Lateral position measurement is described later.

Fig. A-3. A comparator circuit can aid precise timing of a targets passage by a photodiode detector.

It is essential to predict when the target will pass through the center of the reaction chamber for timing of the driver beams to hit the target. A useful method will now be presented for determining the time when a target will pass through the chamber center without calculations or precise knowledge of the target speed. The method requires that the detector positions and chamber center remain fixed relative to each other, or at least the ratios of the distances between them remains constant. Then, assuming constant horizontal speed, the ratio of the time required for the target to pass between the two detectors, to the time required for the target to pass between the second detector and the chamber center is a constant. With this constant ratio, an up/down counter is used to predict target arrival time. When the target passes the first detector, the up/down counter begins counting up at a constant rate. When the target passes the second detector, the up down counter begins counting down at a different rate based on the previously described distance ratio. When the counter again reaches zero, the target should be passing by the chamber center. If early warning of the crossing is needed, some predetermined count other than zero can be used.

We use a digital up/down counter as illustrated in Fig. A-4. As the target passes the first detector, the up/down counter begins counting pulses from a 2 MHz oscillator. When the target passes the second detector, the up/down counter begins counting backwards at

Fig. A-4. A digital up/down counter may be used to help trigger an IFE driver.

1 MHz using a frequency divider chip. When the counter is back to zero, the target is centered in the reaction chamber. The driver timing sequence will be started at an appropriate higher number on the counter.

To this point, we have discussed target position and timing along the direction of motion of the target. As indicated in Fig. A-5, position measurements perpendicular to this motion can also be made with photodiode detectors. The photodiodes shown could be linear arrays of narrow diodes from which a digital output would result. We use single long diodes giving analog signals. For example, if the target were further right in Fig. A-5, less light would reach the upper photodiode. A simple electronic circuit produces an output voltage that is proportional to the light hitting the photodiode.

One set of these lateral position measurement arrays is adequate for predicting the targets final lateral position as it crosses the chamber centerline. Assume that the position of the end of the gun barrel, detectors, and chamber center are constant relative to each other. Also assume that the target is traveling through sufficient vacuum so that its velocity is not disturbed. Then the final deviation in the targets lateral position is directly proportional to the deviation in position at the detectors. The constant of proportionality is simply the ratio of the total flight distance to the distance to the detectors. In the experiment, a second set of detectors is used to verify the final target position is as predicted. We also get accurate information on the small amount of tumble angle that the targets undergo with these detectors based on the slope of the voltage output.

Fig. A- 5. Target position detectors. In part (a) the light source is collimated and the horizontal and vertical measurements are independent. In part (b) the light source is uncollimated so the horizontal and vertical position measurements are coupled.

A.3. Experimental Equipment

A.3.1. Gas Reservoir

We use a 0.1 m³ tank within a mechanical pressure gauge for our gas reservoir. We typically filled this tank to about 16 psia.

A.3.2. Gas Valve

We use a 1/2 in. solenoid operated valve with 10–15 ms response time and flow factor $C_v = 2.6$. We designed and built a valve driver circuit to provide this normally 120 V ac valve with 350 V dc power for a short time interval (about 50 ms) which is controlled by an external pulse generator. The valve driver circuit is attached as Appendix A (see HIFAR Note 485 for appendixes).

A.3.3. Pressure Transducer

We use a 25 psia 5 Volt full scale pressure transducer with approximately 1 ms response time to measure the gas pressure just downstream of the gas valve.

A.3.4. Reloader

The reloader is a 10 shot revolver positioned by a stepping motor with a micro stepping drive. A small vacuum chamber with a transparent cover encloses the reloading mechanism. The holes in the revolver have rifling grooves which are aligned with rifling grooves in the barrel.

A.3.5. Gun Barrel

The gun barrel is about 66 in. long with an inner diameter of 0.631 in. There are four rifling grooves cut 0.002 in. deep and 0.070 in. wide with one revolution each 14 in. The rifling was added to the barrel because without it our targets tumbled excessively. We filed the rifling grooves a little wider near the revolver end to allow easier alignment of the grooves with rifling lands on the target. There is a 3/4 in. tapered section of the barrel to allow easier lateral alignment of the barrel and the revolver. This alignment system leaves room for improvement as we do get occasional jams of the targets as the lands on the target fail to line up with the grooves in the barrel.

A.3.6. Target

The targets are 0.630 in. diameter and 0.725 in. long. We use a piece of rifled gun barrel material as a dye to mold delrin to the outer surface of a 0.600 in. aluminum plug. The leading and trailing edges of the targets are chamfered. Most targets that we have used so far have had solid centers. But to demonstrate the ability to accurately hit targets with an ion beam, we are hollowing out the targets leaving just a 1 mm radius hole on one end through which we intended to pass a 0.3 mm radius beam. Cross sections of the gun barrel and target are shown in Fig. A-6. Instructions for manufacturing targets are attached in Appendix B (see HIFAR Note 485 for appendices).

Fig. A-6. Sections of the target and gun barrel show the delrin wrap, the rifling lands and grooves. As shown on the right, experimental targets are hollow to allow a path to “thread” an ion beam through.

A.3.7. Vacuum Pump

Vacuum pumps are necessary to provide the vacuum. Differential pumping will be necessary for the power plant application. We use a Roots blower backed by an 80 cfm mechanical pump. We have also pumped on the system with a 500 l/s turbo-molecular pumps, but did not shoot the gun while it was operating.

A.3.8. Detection Equipment

Photo diodes are used with light sources. The light sources are light emitting diodes. The second detector has lenses to collimate the beams. If the beams are collimated as shown in Fig. A-5(a), then target position in the horizontal direction does not affect the light reaching the vertical position photodiode. If the beams are not collimated as shown in Fig. A-5(b), then target position in the horizontal direction does affect the light reaching the vertical position photodiode and the horizontal and vertical position measurements are coupled. The detector electronic circuits are attached as Appendix C (see HIFAR Note 485 for appendices).

For the case of non-collimated beams, a computer code helped to determine target position. An example code is included as Appendix D (see HIFAR Note 485 for appendices).

A.3.9. Timing and Control Equipment

Primary control of the experiment and data collection is accomplished with a Macintosh computer and LABVIEW software. Serial commands are sent from the computer to the micro stepping motor driver to control the motor and position the targets for shooting. A pulse generator sends a 50 ms opening pulse to the valve driver that sends power to open and shut the solenoid operated valve. Target speed and position information is collected by a National Instruments PCI 1200 data acquisition Card and displayed by LABVIEW software.

A.3.10. Up/Down Counter

Precise timing information is provided by our up/down counter. The electronic circuit for the up/down counter along with a description of it's operation is included as Appendix E (see HIFAR Note 485 for appendices).

A.3.11. Real Time Target Position Prediction Circuit

Figure A-7 is a simplified schematic of the vertical real time position calculation circuit. The upper part of the schematic uses the vertical position voltage from the second detector. The pulse delay latch circuit is triggered by a 10% voltage drop. It latches the voltage value 110 μ s later as the target center of mass passes the detector. The latched voltage and the reference voltage when the target is not present are passed to an analog to digital converter. The output of the analog to digital converter is passed to a programmable read only memory (PROM) chip which compensates for non linearity in the detector voltage versus position output. The PROM output is then converted to an analog signal which is directly proportional to the position at which the target passed the number 2 vertical position detector.

Fig. A-7. A custom circuit provides a voltage output which is directly proportional to the position at which the target will pass the number 3 vertical detector.

The lower part of the schematic compensates for the difference in gravity fall distance for different target speeds. The counter starts counting from zero when the target passes the number 1 detector and stops counting when the target passes the number 2 timing detector. The PROM then converts this count to a digital signal proportional to the target fall distance. This signal is then converted to a voltage which can be added to the position voltage to give a voltage output which is directly proportional to the position at which the target will pass the number 3 vertical detector. The horizontal real time position calculation circuit is similar to the vertical circuit without the gravity fall compensation. The full circuit schematic diagram is included as Appendix F (see HIFAR Note 485 for appendices).

A.3.12. Baffle Boxes

In addition to the boxes that contain the detectors, 11 baffle boxes were placed in the target path. The baffle boxes are about 30 cm on each side and 10 cm long with movable 2.5 cm diameter holes for the targets to pass through. The baffles were designed to slow the progress of gas so that with a valve shutting behind the target, very little gas would proceed into the vacuum chamber that the ion beam passes through. We experienced difficulty that has not been fully overcome pumping the baffle boxes below 10^{-3} Torr. Reference A-2 gives a discussion of the physics involved and a computer model to calculate gas flow.

A.3.13. Shutter Valve

We designed and built a fast acting shutter valve that together with the baffle boxes was to keep propellant gas out of the ion beam path. This 2 in. valve is opened pneumatically in about 0.5 s, is held open magnetically, and is shut in 13 ms by a spring. The

mechanical and electrical plans for this valve are listed in Refs. A-3 and A-4. Signals from the real time prediction circuit are sent to the shutter valve controller to open this valve to allow the target to pass and begin shutting about 10 ms after the target passes the second detector. Appendix F2 (see HIFAR Note 485 for appendices) shows the electronics required to provide the open and shut signals.

A.3.14. Target Catcher

The targets are caught in a laundry bag and may be reused.

A.3.15. Mechanical Drawings

Much of the system was put together and modified without detailed mechanical drawings and we did not exercise configuration control. However, many useful mechanical drawings and layouts were made by David Beck and are listed in Appendix G (see HIFAR Note 485 for appendices).

A.4. Procedure

A.4.1. Lubrication

We found that when shooting targets without first pumping down the system, that air provided lubrication and the targets moved well through the barrel. However, when shooting into vacuum conditions, the targets would always stick in the barrel. Prior to rifling the barrel we tried drilling holes through the back of the target leading to a ring in the front which would provide an air cushion for the target to ride on. This worked intermittently. We then used oil as a lubricant which worked pretty well but could not be used in a high vacuum environment, nor could oil be used with cryogenic targets. We are currently using dry MoS₂ powder rubbed onto the target surface. This works reasonably well providing less drag and higher target speed than oil. However, using dry lubricant, we have experienced occasional jams of the target as it enters the gun barrel.

A.4.2. Detector Calibration

To calibrate a detector, we place it into a vice on a mill and drape it with cloth to keep exterior light out. We then center a target in the detector and move the detector around the stationary target, reading the output voltage for each position. For uncollimated beams, the horizontal and vertical position measurements are coupled and we get several calibration curves for each detector as illustrated in Fig. A-8. With a properly adjusted collimated beam there is just one calibration curve.

Fig. A-8. Example calibration data for an uncollimated vertical position detector. The output voltage depends on both the vertical and horizontal target position.

A.4.3. Data Acquisition

Voltage data from two horizontal position detectors and two vertical position detectors are each sampled at 50 kHz rate with 12 bits of resolution by two PCI 1200 data acquisition cards giving four arrays of data. LABVIEW downloads these arrays and analyzes the data. A typical plot of part of the voltage array as a target passes a detector is shown in Fig. A-9. Since the output voltage drops when the targets pass by, we collect the array elements with voltage below a threshold value. We discard a few data points at each end and average the rest to get a voltage that is converted to a center of mass target position. We take the maximum and minimum of these points and divide the difference by the number of points involved less 1 to give a magnitude of the voltage slope. If the value of the first saved point is larger than the last saved point, we multiply the slope magnitude by -1 to obtain the actual slope. The voltage slope is the average voltage difference between two adjacent points. We multiply the slope by the average mm/volt value of the detector and divide by distance the target travels between adjacent data points to get the tumble angle of the target. The tumble angle can be multiplied by half the target length to calculate how much the target ends are displaced due to tumble.

We also use an oscilloscope to acquire data from the detectors as well as the pulse generator and pressure transducer. Useful information attained from the pressure transducer is shown in Fig. A-10 taken on a shot with helium propellant gas at 15 psia. The lower trace is the solenoid valve opening signal sent from our pulse generator. The upper trace is from the pressure transducer. The time scale is 20 ms per division. The valve opening pulse is sent at 0 ms. The pressure starts to rise at 6 ms indicating that the valve is opening. The pressure reaches a maximum of 12 psia at 24 ms. The pressure

Fig. A-9. Typical detector voltage output values as a target passes. The slope of the bottom of the plot indicates the amount of target tumble.

Fig. A-10. Pressure measurement just downstream of the gas valve provides interesting and useful data.

starts to drop at 68 ms indicating that the target has left the gun barrel. The valve opening pulse is removed at 72 ms to shut the valve. There is another pressure drop at 88 ms indicating that the valve is shut. Since the target left the barrel about 20 ms before the valve was shut, we subsequently reduced the valve opening pulse to from 72 ms to 50 ms.

A.5. Beam Steering

A.5.1. Steering Electrodes

We intended to use electrostatic steering electrodes to steer the ion beams ± 3 mm in the x and y directions to hit the targets with an accuracy of ± 0.5 mm. We will assume the simplest case of steering electrodes placed immediately downstream of the final magnetic quadrupole (Q-6) as shown in Fig. A-11.

Fig. A-11. Schematic layout of steering electrodes.

The electric field that is required to do the vertical steering will now be calculated. We assume a 120 keV Cs-133 beam. The beam velocity is given by

$$v = \sqrt{\frac{2E_b}{m}} = \sqrt{\frac{240 \text{ keV} (1.6 \times 10^{-16} \text{ J / keV})}{133 \text{ amu} (1 \text{ kg} / 6 \times 10^{26} \text{ amu})}} = 4.16 \times 10^5 \text{ m / s}.$$

The distance the x_3 that the beam is steered in reaching the focal spot is given by

$$x_3 = at_2t_3 = (Eq / m)(l_1 / v)(l_3 / v)$$

So the electric field required to steer the beam 6 mm is

$$E_s = \frac{x_3 m v^2}{q l_1 l_3} = \frac{0.003 \text{ m} (133 \text{ amu}) (1 \text{ kg} / 6 \times 10^{26} \text{ amu}) (4.16 \times 10^5 \text{ m / s})^2}{1.6 \times 10^{-19} \text{ Coul} (0.1 \text{ m}) (0.65 \text{ m})} = 11,000 \text{ V / m}$$

The required steering voltage is $V = E_s l_2 = 440 \text{ V}$ or $\pm 220 \text{ V}$. For horizontal steering the electrodes could be placed proportionately closer together (so that l_2/l_3 is constant) and the steering voltage would be the same as for vertical steering. The target passing with a 10 degree vertical angle will not hit the steering plates.

The flat steering electrode design may have to be modified to achieve adequate field uniformity. An alternative steering electrode design is discussed in HIFAR Notes #12 and #14.

It will now be shown that the electric field due to the image charge effect does not excessively affect the beam focus. Fig. A-12 shows the 15 cm radius 100 μ A ion beam passing between two steering electrodes located 40 cm apart with the first image charges on the outside of the plates. We will consider the conservative worst case effect of the image charge to the left on beam particles nearest to the left electrode. The total effect on these particles will be reduced by the remaining image charges not considered. Also charges further from the edge will be deflected less.

Fig. A-12. The ion beam passing through two electrodes will be slightly defocused due to "image charges" attracted to the electrodes by the beam.

The field at the surface of the left edge of the beam cylinder charge due to the left image charge can be found easily by Gauss's law assuming an infinite conductor and infinitely long charge distribution. The actual field will be less.

$$E_i = \frac{q}{\epsilon_0 2\pi r l} = \frac{I}{2\pi \epsilon_0 r v} = \frac{(10^{-4} \text{Coul} / \text{s})}{6.28(8.84 \times 10^{-12} \text{Coul}^2 / \text{Nm}^2)(0.025 \text{m})(4.2 \times 10^5 \text{m} / \text{s})} = 171 \text{ V} / \text{m}.$$

This field will deflect the charge proportionately less than the 22100 V/m steering voltage.

$$\Delta x_i = \Delta x_3 \frac{E_i}{E_s} = 6 \text{ mm} \frac{171 \text{ V} / \text{m}}{22100 \text{ V} / \text{m}} = 0.046 \text{ mm}.$$

When compared to the 0.3 mm radius spot size and considering that most particles won't be deflected as much as edge particles and that the other image charges will reduce the total image charge effect, the net image charge deflection from the steering electrodes is acceptable.

A.5.2. Steering Amplifier

I designed and built an amplifying circuit which can amplify the output from the real time target position prediction circuit for beam steering purposes. It has an adjustable input offset voltage (currently 3.1 V) and adjustable amplification (currently 340) which is linear within 1%. The linear output range is -540 V to +480 V. Four identical circuits (2 horizontal and 2 vertical) could be used to energize steering electrodes real time. Since the voltage is greater than the ± 220 V calculated earlier, we could obtain greater steering of higher energy beams and possibly use a wider steering electrode separation. The beam steering circuit diagram is included as Appendix H (see HIFAR Note 485 for appendices). A ± 550 V and ± 12 V supply was also built for input power to the steering amplifier.

A.6. Conclusions

If we decide to continue this experiment one day to demonstrate hitting targets on the fly with an ion beam, this information will be useful.

A.7. References

- A-1 R.W. Petzoldt, "IFE Target Injection and Tracking Experiment," *Fusion Technology* **34**, 831 (1998).
- A-2 R.W. Petzoldt, "Gas Baffle Ion Beam Protection — Rev 2," 8 January 1998, (HIFAR Note #484).
- A-3 B. Ghiorso, HIF final focus fast valve firmware, LBNL Drawing 31W013 (1998).
- A-4 B. Ghiorso, HIF final focus fast valve control, LBNL drawing 31W012 (1998).

Appendix B: Estimates of Stress Induced by DT Temperature Rise

B.1. Introduction

IFE targets will be heated from the outside by radiation and gas convection as they pass through a reaction chamber. Targets such as illustrated in Fig. B-1 are particularly susceptible to target surface heating since cryogenic DT is so close to the surface. There will be a significant temperature gradient across the target causing expansion and possible melting of the outer DT. How will this affect the capsule and especially the inner DT? We know that uniform temperature changes of 0.5 K cause surface roughness. One would expect that a similar temperature rise confined in the outer fuel could have a similar effect. I cannot fully answer this question at this time but will take a "back of the envelope" look at some effects.

Fig. B-1. Recent NRL target design with DT in the ablator.

B.2. Temperature Rise of Solid DT

We will first consider the linear expansion due to a 1 K temperature increase from 18 K to 19 K. The estimated molar volume of 0 K DT is 19.29 cm^3 (Hydrogen properties are from Ref. B-1). The molar volume at other temperatures is given by

$$V_s(T) = V_s(0) + 1.68 \times 10^{-6} \times T^{4.3} \quad .$$

So the fractional change in volume going from 18 K to 19 K is

$$\frac{V(19K) - V(18K)}{V(0K)} = \frac{1.7 \times 10^{-6} (19^{4.3} - 18^{4.3})}{19.3} = 0.006 \quad .$$

The linear expansion should average one third of this value or 0.002. Young's modulus for D₂ at 16.4 K is 40 MPa, the yield stress is 50 kPa, and Poisson's ratio is 0.3. The approximate stress induced if the DT were not allowed to expand would be

$$\sigma_{DT} = \frac{\epsilon_{DT} E_{DT}}{1 - 2\nu_{DT}} = \frac{0.002(40MPa)}{(0.4)} = 200kPa \quad .$$

Since the triple point of DT is 1 degree higher than D₂, we might guess that the yield stress of DT is similar to D₂ (with DT at 1 degree higher temperature). Actually, the heated DT, only partially restrained by the outer plastic, will expand reducing the compressive stress. So a 1 K differential temperature leads very roughly to the yield stress in the fuel. Numerical modeling is recommended to calculate the stresses more accurately. We can't determine from this whether cracking or surface roughening will result.

B.3. Melted DT Effects

DT expands 14% from 19.9 cm³/mole to 22.6 cm³/mole while melting at the triple point temperature and pressure. The pressure increase from expansion will not cause a large increase in the melting temperature because the freezing pressure rises rapidly with temperature above the triple point of 19.7 K and 2×10⁴ Pa. At 20 K the freezing pressure is 1.2 MPa, and at 22 K it is 11 MPa. Moderate pressure increases do not eliminate the expansion due to melting. At 22 K and 11 MPa, the liquid molar volume is 21.8 cm³/mole, still 10% above the triple point molar volume. Young's modulus for cryogenic Mylar is about 7 GPa (175 times greater than Young's modulus for D₂ at 16.4 K).

If some DT melts in the target shown in Fig. B-1, a 1.5 micron thick plastic shell must contain the expansion of a thin layer of DT liquid around a few hundred micron thick solid DT layer. The force per unit length of each of these shells is given by (Ref. B-2) $N = (1/2) pr$ where p is the pressure and r is the radius of the assumed thin shell.

If for example, a 50 micron thick layer melted, the radial expansion would be approximately 7 microns. The displacement of each shell is roughly inversely proportional to the product of Young's modulus and the shell thickness. The solid DT layer thickness is 400 microns. We don't know the effect of the foam on this problem. It will certainly break up the DT crystal structure. We proceed assuming the foam/DT has

the same strength as DT. The ratio of the plastic radial displacement to the DT radial displacement is roughly

$$\frac{\Delta r_p}{\Delta r_{DT}} \approx \frac{400 \mu\text{m}(40 \text{ MPa})}{1.5 \mu\text{m}(7000 \text{ MPa})} = 1.5$$

so we expect the plastic to move outward roughly 4 microns and the DT to move inward roughly 3 microns. The strain in each direction of the plastic shell is approximately $4/2250=0.0018$. This is not enough to yield or fracture a Mylar shell (cryogenic Mylar can stretch about 4% before breaking). Assuming Poisson's ratio of 0.3, the plastic stress is approximately

$$\sigma_p = \frac{\epsilon_p E_p}{1 - \nu_p} = \frac{0.0018(7 \text{ GPa})}{0.7} = 18 \text{ MPa} .$$

The liquid DT pressure is

$$p = \frac{2N}{r_p} = \frac{2\sigma_t r_p}{r_p} = \frac{2(18 \text{ MPa})(1.5 \mu\text{m})}{2250 \mu\text{m}} = 24 \text{ kPa} .$$

This is not enough pressure to significantly affect the melting temperature and happens to be quite near the triple point pressure of 20 kPa.

The DT stress is approximately

$$\sigma_{DT} = \frac{\epsilon_{DT} E_{DT}}{1 - \nu_{DT}} = \frac{3(40 \text{ MPa})}{2000(0.7)} = 85 \text{ kPa}$$

which exceeds the D₂ yield stress of 50 kPa.

Reference B-2 gives the critical pressure at which elastic buckling is expected to occur as

$$p_{cr} = \frac{2Et^2}{\sqrt{3(1-\nu^2)}r^2} = \frac{2(40 \text{ MPa})(400)^2}{\sqrt{3(1-0.3^2)}(2000)^2} = 2 \text{ MPa}$$

which is much greater than the actual pressure of 24 kPa. So buckling is not expected below the yield stress.

There is significant energy absorbed in melting DT. The heat of fusion of DT at the triple point is approximately 250 J/mole compared with the specific heat capacity at that temperature of 21.4 J/mole•K. Thus about 12 times more heat is absorbed in melting DT than increasing it's temperature 1 K near the triple point.

B.4. Outer Layer DT Gas Pressure

We noted in the previous section that 24 kPa (or even 20 kPa) of melted DT pressure is probably enough to cause yielding of the DT beneath it. If the temperature rises above 20 K, the vapor pressure will increase above 20 kPa. At 22 K the DT vapor pressure is 47 kPa, at 24 K the DT vapor pressure is 91 kPa and D₂ pressure is 112 kPa. Once the vapor pressure exceeds the liquid pressure, bubbles could start forming in the liquid. Some degree of superheat is expected prior to boiling. Once boiling begins, the gas bubbles will probably cause implosion instabilities.

B.5. Conclusions

The above calculations all assumed uniform heating. If the heating is not uniform on the surface, the stresses will be greater and the capsule/DT will expand to a non-spherical shape. It is not obvious from the above calculations that a 1 K temperature rise or 50 microns of melting cannot be tolerated. However, once the temperature exceeds about 20 K, gas bubbles can be expected to form that will degrade target performance. Also, our negative experience with uniform DT heating (over relatively long time periods) leads me to expect similar problems with rapid heating as well.

B.6. Reference

- B-1 P. Clarke Souers, "Hydrogen Properties for Fusion Energy," University of California Press, Berkeley, California (1986).
- B-2 W. Flügge, *Stresses in Shells*, 2nd Ed., Springer-Verlag, New York (1973) pp. 500-505.

Appendix C: Cooling and Stress Analysis of Direct Drive Targets

C.1. Introduction

A cooling/stress analysis was done on the proposed direct drive target design illustrated in Fig. C-1. Thermal stresses were computed during a cooldown from the triple point to selected temperatures. The analysis was performed with the ANSYS finite element code.

Fig. C-1. Direct drive concept (NRL design).

C.2. Background

Prior to injection, the target will be cooled from the layering temperature (near the triple point of 19.79 K) to a temperature around 18 K. During the cooling process, stresses will be induced in both the D-T ice and in the plastic shell. These stresses are a result of differences in the thermal expansion coefficients of the D-T and the shell material. **The D-T expansion coefficient is 100 times greater than that for the shell material.**

For the analysis the cooling process was considered to be sufficiently slow that temperature gradients do not exist throughout the target.

C.3. Results: Cooldown

The Figs. C-2 and C-3 show the stress in the hoop direction when the target is cooled down 0.5 K and 0.79 K, respectively.

Fig. C-2. The maximum stress in the target does not quite reach the yield point for half a degree cooldown.

Fig. C-3. The yield stress is exceeded at the inner surface for a cooldown of 0.79 K.

Figure C-3 shows that the yield stress is exceeded by about 2500 Pa at the inner surface for a cooldown of 0.79 degrees. **Given these results and experimental results from a cooling analysis performed on a similar target (Ref. C-1), it is reasonable to conservatively estimate yielding during cooldown to occur at a temperature decrease of 0.5 K.**

These results are heavily dependent on the mechanical properties of the D-T and the shell material. A listing of the assumed properties along with plots showing the temperature dependence of the D-T yield stress and elastic modulus are included later in this appendix.

A separate calculation was performed to validate the work done with ANSYS. The equations used are listed below:

$$\delta_{DT} = b \cdot \alpha_{DT}(T_{DT} - T_{ref}) = DT \cdot radial \cdot displacement$$

$$\delta_{shell} = b \cdot \alpha_{shell}(T_{shell} - T_{ref}) = Shell \cdot radial \cdot displacement$$

$$F_{DT} = \frac{b}{E_{DT}} \left[\frac{a^3 + 2b^3}{2(b^3 - a^3)} \cdot (1 - \nu_{DT}) - \nu_{DT} \right] = Interface \cdot force, \frac{m}{Pa}$$

$$F_{shell} = -\frac{b}{E_{shell}} \left[\frac{c^3 + 2b^3}{2(c^3 - b^3)} \cdot (1 - \nu_{shell}) - \nu_{shell} \right] = Interface \cdot force, \frac{m}{Pa}$$

$$P_{interface} = -\frac{(\delta_{DT} - \delta_{shell})}{F_{DT} - F_{shell}} = Interface \cdot pressure, Pa$$

$$\sigma_{hoopDT} = P_{interface} \cdot \frac{3b^3}{2(b^3 - a^3)} = Hoop \cdot stress \cdot at \cdot inner \cdot surface, Pa$$

$$a = inner \cdot radius = 1500 \mu m$$

$$b = outer \cdot DT \cdot radius = 1951 \mu m$$

$$c = outer \cdot target \cdot radius = 1951.8 \mu m$$

$$E = elastic \cdot modulus, Pa$$

$$\nu = Poisson's \cdot ratio$$

The displacement is first calculated for a given temperature drop. Once this is done the interface force and the interface pressure can be found. The interface pressure is the maximum radial stress or, the radial stress which occurs at the interface between the D-T and the shell. The hoop stress at the inner surface of the D-T is a function of the interface pressure and the target geometry. This model predicts a maximum hoop stress of about 12000 Pa for a 1 degree temperature drop. The ANSYS model outputs a value of about 15000 Pa.

From the above equations it is apparent that the stress in a target is a strong function of the geometry. The following plot illustrates how the hoop stress in the D-T changes as a function of the ratio of the inner D-T radius to the outer D-T radius. The stresses were determined for a cooldown to 19 K.

Figure C-4 shows that as the D-T layer thickness decreases, so does the maximum hoop stress at the inner D-T surface.

Fig. C-4. Effect of DT layer thickness on maximum hoop stress.

The thickness of the capsule also influences the stress in the D-T as shown in Fig. C-5 below.

Fig. C-5. Effect of capsule thickness on maximum hoop stress.

For targets near the size of the NRL concept, capsule thicknesses of less than one micron are needed to keep the maximum hoop stress below 12000 Pa for a cooldown to 19 K.

C.4. D-T Mechanical Properties

Both the yield strength and the elastic modulus for D-T ice at temperatures near the triple point were estimated using published data for D₂ and adjusting the values based on the triple point temperature difference between D₂ and D-T (Ref. C-3). Since the temperature range of the data points did not reach to 19.79, a curve fit was generated using the available points and then assuming that both the yield strength and elastic modulus equal zero at the T.P. A polynomial equation, as shown in Figs. C-6 and C-7, was found to be the best fit in both cases.

Fig. C-6. DT elastic modulus as a function of temperature.

Fig. C-7. DT yield stress near the triple point.

Even though the curve fit equations show a correlation coefficient of 1, the fit is not exact. If 19.79 is entered, the result for both yield stress and elastic modulus will be greater than zero.

It is uncertain how the mechanical properties of D-T change near the triple point. It is possible that the yield stress drops exponentially and that the D-T is far weaker than shown in the previous figures. Data nearer the triple point is needed.

C.5. Conclusions

Yielding during a slow cooldown can be expected to occur when the temperature of the D-T drops 0.5 K below the triple point temperature of 19.79 K.

The hoop stress in the D-T decreases with D-T layer thickness.

A decrease in capsule thickness can decrease D-T hoop stress significantly only for very thin capsules.

C.6. References

- C-1 Beta Layering in Spheres. ICF Target Component Fabrication and Technology Development Support, FY99 Mid-Year Review, San Diego, 1999.
- C-2 R.J. Roark, *Roarks Formulas for Stress and Strain*. McGraw-Hill, New York, 1988.
- C-3 P.C. Souers, "Hydrogen Properties for Fusion Energy," University of California Press, Berkeley, 1986.

Appendix D: Direct Drive Target Heating Calculations Using ANSYS

D.1. Abstract

A finite element analysis (FEA) was performed on a prospective direct drive target design to determine the temperature rise throughout the target when subjected to an external heat flux. The following paragraphs highlight the details of the ANSYS programming.

D.2. Geometry

The target was modeled in ANSYS as two dimensional, axi-symmetric. The plastic (polystyrene) capsule was 0.8 microns thick while the D-T fuel and D-T/foam ablator had a combined thickness of 451 microns.

D.3. Loads

The load was applied as a constant heat flux on the outer surface of the plastic shell. The magnitude of the flux was calculated separately for a range of injection velocities (see Appendix E). The flux load duration was equal to the injection time or the chamber radius (6.5 m) divided by the injection speed. The model was solved as a transient analysis in order to account for the time dependent nature of the loading.

An initial condition load (temperature equal to 18 K) was placed on the entire model prior to each solution.

D.4. Mesh Geometry

A mapped mesh was used for both the plastic shell and the D-T ice layers. The element type was a four-node quad with a temperature degree of freedom (Plane55). Because of the mapped mesh, all of the elements were rectangular in shape. The geometry is shown in Fig. D-1.

Fig. D-1. Model geometry.

The shell cannot be seen due to its small thickness relative to the D-T layer.

D.5. Material Properties

The material properties used for the shell were those of polystyrene taken from the Sombrero report. The D-T ice properties were taken from "Hydrogen Properties for Fusion Energy" by P.C. Souers.

D.6. Results

The results of the analysis were temperatures on nodes along the radius of the model. The outer ablator temperature was considered to be critical since it heats up the most during injection and will be the first portion of the D-T to either liquify or vaporize at the triple point of 19.79 K. (*Temperature rise results are given in Fig. 3-2 of this report.*)

Appendix E: Estimating the Surface Heat Load to a Direct Drive Target During Injection

Convective heat transfer to the IFE target can be modeled using continuum correlations only when the gas density in the chamber is large enough to yield flows with Knudsen numbers of 0.1 or less. The Knudsen number for a SOMBRERO target chamber with 0.5 Torr of xenon gas is 0.1 at 400m/s for a target with a diameter of 4 mm. The Knudsen number decreases with injection speed. For greatest accuracy the Knudsen number should be as small as possible which indicates a large gas density and ensures continuum flow. The following equation was used to model subsonic convective heat transfer to the target under continuum conditions:

$$Nu_D = 2 + (0.4 Re_D^{1/2} + 0.06 Re_D^{2/3}) Pr^{0.4} \left(\frac{\mu}{\mu_s} \right)^{1/4}$$

$$0.71 < Pr < 380$$

$$3.5 < Re_D < 7.6E^4$$

$$1.0 < \left(\frac{\mu}{\mu_s} \right) < 3.2$$

As the flow becomes more rarefied and therefore less like continuum flow a correction factor must be used with the above equations. This correction factor accounts for a slip boundary condition (non zero flow velocity at the gas-surface interface) which is present in rarefied flows with Knudsen numbers greater than 0.01. The following relationships were used in conjunction with the above equation to evaluate convective heat transfer in all flow regimes. It should be noted that if the flow is sufficiently dense the correction factor is essentially equal to one. As the Knudsen number increases so does the correction factor.

$$Nu_{slip} = \frac{Nu_{cont}}{1 + \zeta}$$

$$\zeta = 1.225 \sqrt{\gamma} \frac{2 - \alpha}{\alpha} \cdot \frac{2\gamma}{\gamma + 1} \cdot \frac{Ma}{Re \cdot Pr}$$

$$Nu_{cont} = \text{continuum} \cdot \text{heat} \cdot \text{ratio}$$

$$\alpha = \text{accommodation} \cdot \text{coefficient}$$

The above equations yield the average Nusselt number around the spherical surface. In reality there would be 'hot spots' which cannot be evaluated with these equations.

As with all such equations, there are associated criteria which establish the normal expected range of validity. Unfortunately, we have no model which meets all criteria for the conditions of cryogenic target injection into a SOMBRERO-like chamber. In this case of heat transfer across a large temperature gradient, the ratio of the viscosity of the freestream fluid to the fluid near the wall will be about 70 for a target at 18 K and a chamber at 1758 K. This exceeds the allowable viscosity ratio for this equation by a factor of approximately 20. In addition, the Prandtl number is below the allowable limits by about 18%. The Reynolds number is only out of range for injection speeds less than 20 m/s.

The following plots show surface heat flux for a direct drive target, 3.904 mm in diameter, as a function of injection velocity. The data in Fig. E-1 was calculated using the above equations. Figure E-2 was calculated using the heat transfer relation from the original Sombrero report along with the slip correction as defined above. The Sombrero heat transfer relationship is shown below.

$$Nu_{sombbrero} = 0.37 Re^{0.6}$$

$Nu_{sombbrero}$ = Continuum Nusselt number from Sombrero report.

Fig. E-1. Incident heat flux using Whitaker continuum equation with slip flow correction.

Fig. E-2. Incident heat flux using SOMBRERO equation with slip reduction factor.

Overall, the heat flux as calculated with Whitaker is higher than when calculated with the Sombbrero equation.

A computational fluid dynamics (CFD) analysis was performed to determine the heat flux distribution around the target. Figure E-3 shows the heat flux on the target surface due to convection only. The program used to perform the analysis was ANSYS Flotran. It is based on a Navier-Stokes solver which is only accurate for continuum flow. For this reason a CFD analysis using this program could not be performed at pressures lower than 0.5 Torr. It is possible to use other types of programs, such as direct simulation, to construct a CFD model valid under more rarefied conditions.

As expected, the heating is greatest at the leading edge of the target. The average heat flux was also calculated and is included in Table E-1.

From the table it is apparent that the CFD results are relatively close to the results from the Whitaker equation whereas the Sombbrero equation yields much lower heat flux values.

Fig. E-3. Heating distribution for direct drive IFE target diameter = 3.904 mm, $T = 1758 \text{ K}$, $P_{\text{xenon}} = 0.5 \text{ Torr}$.

Table E-1
Average Heat Flux at 0.5 Torr

Injection Velocity (m/s)	Heat Flux - Whitaker (W/cm^2)	Heat Flux - Sombbrero, (W/cm^2)	Heat Flux - CFD (W/cm^2)
100	7.10	2.08	5.79
200	9.33	3.15	7.60
400	12.76	5.01	12.80

References

- E-1 F.P. Incropera, D.P. DeWitt, *Introduction to Heat Transfer*, John Wiley and Sons, New York, New York (1996) p. 349.
- E-2 D.K. Eberly, "Forced Convection Heat Transfer From Spheres to a Rarefied Gas," University of California, Berkeley, California Technical Report HE-150-140 (1956).

Appendix F: Estimates of the Upper Limits for Direct Drive Target Heating During Injection

Very light weight ($m \sim 4$ mg, $r_T \sim 2$ mm radius) direct-drive targets will be injected into a reaction chamber of radius $r_c \sim 6$ m at high speed (200–2,000 m/s). The target might pass through gas density of xenon or $4 \mu\text{g}/\text{cm}^3$ at $\sim 1,800$ K (pressure = 0.5 Torr at standard temperature), which is in place to protect the chamber wall from x-rays and debris. The question to be answered here is how much this gas will it affect DT temperature. The effect of the gas drag on the target was covered in Section 3.1.4.

The mass of gas in the target path is

$$m = \pi r_T^2 \rho r_c = 3.14(0.2\text{cm})^2(4 \times 10^{-3} \text{mg}/\text{cm}^3)(600\text{cm}) = 0.3\text{mg}.$$

A rough upper limit to the amount of target heating can be obtained by assuming that the gas molecules hit the target with the speed of injection and stick transferring all of their energy to the capsule. As indicated in Ref. F-1, this is what happens for a sufficiently cold target which is acting as a small moving cryopump. However, Ref. B-2 indicates that above a critical molecular kinetic energy corresponding to roughly 400 K temperature, molecules usually do not stick to a cold surface. In any case, this model does not apply after the surface of the target heats above the condensation temperature of the gas. The energy transferred is given by $E = mv^2/2$. For example, using the previously estimated mass of 0.3 mg and $v = 1,000$ m/s, the energy transferred would be 0.15 Joules. At 18 K, the specific heat of DT is 3,500 J/kg·K. If this amount of energy were absorbed in 4 mg of DT,

$$\Delta T = \frac{E}{mc} = \frac{0.15 \text{ J}}{4 \times 10^{-6} \text{ kg}(3500 \text{ J/kg} \cdot \text{K})} = 11 \text{ K}.$$

This heat would not be distributed uniformly in the fuel. It would primarily heat the leading side of the target, and would have to be conducted through the fuel. For target designs that have DT just a few microns from the surface, the temperature rise in the DT near the surface would be much greater.

The preceding calculation assumes that the target speed is much greater than the thermal speed of the xenon atoms. This root mean square speed is given by

$$v_{rms} = \sqrt{\frac{3kT}{m}} = 158 \text{ m/s} \sqrt{\frac{T}{M}} = 158 \text{ m/s} \sqrt{\frac{1800}{131}} = 586 \text{ m/s}.$$

For injection speeds higher than this, relatively few gas molecules will hit the backside of the target and these will carry relatively little kinetic energy compared to the molecules striking the front surface. However, as the target speed is reduced, more gas molecules will reach the target due to their thermal motion while the target is in the chamber for a longer time period. In the case of free molecular flow the number reaching the target due to thermal motion is equal to the number contained in a volume

$$V = \frac{\bar{v}_{th}}{4} 4\pi r_T^2 t = \bar{v}_{th} \pi r_T^2 \frac{r_c}{v_T} .$$

This is

$$\frac{\bar{v}_{th}}{v_T}$$

times the volume swept by the target motion in the chamber. Since $\bar{v}_{th} = 0.92v_{rms}$, the total energy transferred from the gas to the target is given approximately by

$$\begin{aligned} E_{cond} &= m_g \left(v_T^2 + v_{rms}^2 \left(1 + \frac{0.92v_{rms}}{v_T} \right) \right) / 2 \\ &= 3 \times 10^{-7} \text{ kg} \left((1000 \text{ m/s})^2 + (586 \text{ m/s})^2 (1 + (0.92)(586)/1000) \right) / 2 = 0.23 \text{ J} \end{aligned}$$

which is about 50% greater than the value neglecting the gas thermal motion. This simple model actually assumes free molecular flow and might not be very accurate in our higher density gas. More detailed analysis of the effect of nearby gas atoms on each other and an accurate assessment of condensation and energy transfer in collisions is necessary for an accurate heat transfer prediction.

References

- F-1 R.A. Haefer, *Cryopumping Theory and Practice*, Clarendon Press, Oxford (1989) p. 3.
- F-2 M.M. Eisenstadt, "Condensation of Gases During Cryopumping — The Effect of Surface Temperature on the Critical Energy for Trapping," *J. Vacuum Science and Technology* **7**, (1970) 479.
- F-3 J.H. VanSant, "Conduction Heat Transfer Solutions," Lawrence Livermore National Laboratory Report UCRL-52863 Rev. 1 (1983).

Appendix G: Heating of Indirect Drive Target

G.1. Abstract

A finite element analysis was performed using ANSYS to determine the heating characteristics of a proposed indirect drive IFE target. The target was subjected to a series of temperature loads which simulated injection by a gas gun into a target chamber at 900 K. Temperature profiles of the target were then generated.

G.2. Finite Element Results

Figure G-1 is an illustration of the close-coupled, indirect drive target analyzed in this report. For a complete listing of dimensions and materials see Ref. G-1.

Fig. G-1. ANSYS model of indirect drive target

The ANSYS finite element code was used to evaluate the temperature distribution throughout the indirect drive target for an injection speed of 100 m/s. The loading was done as follows:

300 K temperature applied to the backside of the target to simulate the firing of the gas gun injector.

Coast period, no load applied. This simulates the targets flight through the remainder of the injection and tracking system.

900 K applied to the entire target surface. Simulates heat transfer by radiation to the target surface.

All heat loads were modeled as **temperatures applied at the target surface**. This was done because the surface properties of the target, such as reflectivity and surface conductance, were unknown. A temperature boundary condition is a very conservative estimate of the actual loading condition.

The results of the analysis show that the fuel pellet remains unheated upon reaching the center of the target chamber. The following figures illustrate the finite element results.

Figure G-2 shows the temperature distribution at the back of the target at the moment it reaches the center of the chamber. Appreciable heating is limited to the first 1 mm of material measured from the free end along the axis of symmetry. The 2 mm AuGd "window" essentially blocks all of the heat transfer coming in from the ends. The peak surface temperature is shown as 900 K. If the surface reflectance was known for radiation at the target chamber temperature of 900 K, this analysis could be repeated with a radiative heat load instead of a temperature load at the surface. Reflection of incident radiation could considerably lower the surface temperature.

Fig. G-2. Heating of the backside of the target. Injection speed is 100 m/s.

Figure G-3 shows the temperature distribution from the top down toward the line of symmetry. Heating is confined to the first 1.25 mm. The knee in the curve represents a transition from the FliBe outer layer (relatively high thermal conductivity) to the AuGd inner layer (relatively low conductivity). Quartz glass properties were used in place of FliBe. The outer temperature of the target exceeds the melting point of FliBe, 730 K, but with a reflective coating applied at the surface, this surface temperature could be lowered as shown in Fig. G-4.

Fig. G-3. Heating of the topside of the target. Injection speed is 100 m/s.

Fig. G-4. Heating of the topside of the target. Injection speed is 100 m/s.

When the surface reflectance is raised to 90% and the heat load is applied as a uniform surface heat flux, the outer wall temperature of the target is lowered considerably. A maximum surface temperature of about 28 K is far below the melting point of FLiBe.

Figure G-5 shows a temperature distribution immediately after the acceleration phase of the injection.

Fig. G-5. Heating of the backside of the target during acceleration injection. Velocity speed is 100 m/s.

G.3. Assumptions

The bulk of the materials referenced in the target design do not yet exist. Therefore, it was necessary to make some assumptions with regard to material properties:

Properties for CD₂ plastic foam were taken to be those of polystyrene under the same conditions. Available data for thermal conductivity does not include values at the required densities. Therefore it was necessary to extrapolate. The extrapolation was carried out under the assumption that the change in thermal conductivity was proportional to the change in radiative absorption of heat, α , through the low density foam. The reason for this approximation is that as density decreases, heat flow through conduction decreases while heat flow due to radiation increases. At sufficiently low densities radiative heat transfer becomes dominant (Ref. G-2). The formulation of absorption is as follows:

$$\alpha = 1 - e^{-KL}$$

L is the path length or material thickness and K is the extinction coefficient. The magnitude of the extinction coefficient was considered to be proportional to the magnitude of the foam density since absorption is a function of the density of the absorbing medium (Ref. G-3). The analysis proceeds by assigning an arbitrary value of K to PS foam at 32 kg/m³ for which there is tabulated conductivity data. The absorption is then calculated. For lower densities, K and α are re-calculated, based on the densities, and α is used to yield a ratio between the thermal conductivity of the high density (tabulated) PS and the lower density PS. The thermal conductivity is the highest at the lowest density because thermal radiation has become dominant in the overall heat transfer.

The low density metals used in the design (AuGd, Fe, Al) were considered to be multi-layered foil insulation consisting of alternating layers with a layer thickness of 0.006 mm. A chart from Ref. G-4, page 399, was used to evaluate the conductivity for aluminum insulation at the required densities and this was then scaled to yield the conductivities of the other metals.

The AuGd alloy properties were taken as those of gold only.

All thermal loads were applied as surface temperatures. This gives a “worst case” loading situation.

The properties of FliBe were approximated as being those of quartz glass.

A separate analysis was performed to verify the results of the finite element analysis. A 2 mm slab of AuGd alloy, density of 1000 kg/m³, was loaded with 900 K on its surface for a total duration of 0.102 s. This load time corresponds to the target injection time at an injection velocity of 100 m/s. The initial temperature was 18 K.

$$T = T_s - (T_s - T_0) \cdot \operatorname{erf} \left[\frac{x}{2\sqrt{\alpha \cdot t}} \right]$$

$T = \text{Temperature at } x, \text{ Kelvin}$
 $T_s = \text{Surface Temperature, Kelvin}$
 $T_0 = \text{Initial Temperature, Kelvin}$
 $x = \text{Depth, m}$
 $t = \text{time, s}$

$$\alpha = \frac{k}{\rho \cdot c}, \text{ Thermal diffusivity, } \frac{\text{m}^2}{\text{s}}$$

$$\rho = \text{Density, } \frac{\text{kg}}{\text{m}^3}$$

$$c = \text{Heat capacity, } \frac{\text{J}}{\text{kg} \cdot \text{K}}$$

$$k = \text{Thermal conductivity, } \frac{\text{W}}{\text{m} \cdot \text{K}}$$

Using the above relations, the temperature across the AuGd ends was found to be 18 K after 0.102 s (the injection time). This results mainly from the low value of thermal conductivity of the multi-layered insulation ($k = 3.79 \times 10^{-5} \text{ W/mK}$). At this conductivity it would take 67 s for the inner surface to reach 18.5 K. In order to reach 18.5 K at the inner surface in 0.102 s, the conductivity would have to be raised to $4.44 \times 10^{-2} \text{ W/mK}$, an increase of over 1000 times.

If the conductivity of the AuGd was raised, the D-T capsule at the center is still protected by a low density/low conductivity foam.

G.4. Conclusions

The D-T fuel pellet experiences no appreciable heating during injection.

The heat transfer through the target is highly dependent on the thermal conductivity of the materials. It is important that the thermal properties for all materials used be evaluated empirically.

G.5. References

- G-1 D.A. Callahan-Miller, M. Tabak, "Increasing the Coupling Efficiency in a Heavy-Ion Inertial Confinement Fusion Target," Lawrence Livermore National Report UCRL-JC-133231.
- G-2 *Encyclopedia of Polymer Science and Technology*, John Wiley and Sons (1985).
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- G-4 R.F. Barron, *Cryogenic Systems*, Oxford University Press, New York (1985).

Other Materials Property References

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Appendix H: Preventing Flibe From Diffusing Into Target Injection Region

The HYLIFE-II reactor has Flibe with a molecular density of about $3 \times 10^{13}/\text{cm}^3$. We will need a 1 cm radius opening for the targets to pass into the reaction chamber. A rough estimate of the mass of Flibe vapor that would pass through such an opening per year with this gas density is approximately given by

$$\begin{aligned} m &= \rho \bar{v} A t / 4 = n M \sqrt{\frac{8RT}{\pi M}} \pi r^2 \left(\frac{t}{4} \right) \\ &\approx 3 \times 10^{19} / \text{m}^3 (0.04 \text{ kg/mol}) (1 \text{ mol} / 6 \times 10^{23}) \\ &\quad \times \sqrt{\frac{8(8.3 \text{ J/mol} \cdot \text{K})(900 \text{ K})}{3.14(0.04 \text{ kg/mol})}} (3.14)(0.01 \text{ m})^2 \frac{3 \times 10^7 \text{ s}}{4} \\ &= 3 \text{ kg} \end{aligned}$$

It is desirable to keep Flibe out of the target injection area, especially out of the gun barrel and off the target tracking surfaces. As indicated in Fig. H-1, one way to achieve this would be to have a counterflow of gas in a tube leading to the chamber which would minimize Flibe diffusion. The end of the tube would be shut during pressure pulses from the fusion micro-explosions.

Fig. H-1. Flibe vapor entering the target injection and tracking area is minimized by closing a shutter during the fusion pressure pulse and providing a counter flow of helium gas while the shutter is open.

We desire a rough estimate of the amount of gas flow that would be required to minimize the Flibe entering the injection area. Reference H-1 gives the concentration of a gas diffusing through another gas moving with velocity v as

$$c = c_0 \exp\left[-\left(\frac{v}{D}\right)x\right] .$$

The diffusion coefficient D for hydrogen or helium with heavy gases is typically about $0.5 \text{ cm}^2/\text{s}$ at standard temperature and pressure. D varies inversely with the pressure and increases with the temperature to the 1.5–2.0 power. The power of 1.5 to 2.0 comes from density decrease (1.0), molecular speed increase (0.5), and slightly reduced effective collision diameter (0.0–0.5) at higher speeds. The diffusion can be reduced by a factor of 10^6 by setting the $vx/D = 14$. Assuming a gas pressure of 1 Torr, a temperature of 800 K to keep Flibe from freezing to the tube surface, and $x = 100 \text{ cm}$, gives

$$D \approx 0.5(760)\left(\frac{800}{273}\right)^{1.75} = 2,500 \text{ cm}^2/\text{s}$$

and $v = 14(2500 \text{ cm}^2/\text{s})/(100 \text{ cm}) = 350 \text{ cm/s} = 3.5 \text{ m/s}$. The mass flow rate of helium through the tube is about

$$\dot{m} = \rho v A \approx \frac{PM}{RT} v \pi r^2 = \frac{1 \text{ Torr}(0.004 \text{ kg/mole})}{8.3 \text{ J/mole} \cdot \text{K}(800 \text{ K})} \frac{10^5 \text{ Pa}}{760 \text{ Torr}} (3.5 \text{ m/s})(3.14)(0.01 \text{ m})^2 = 0.1 \text{ mg/s}$$

which is much less than the several mg/s that is being produced in fusion reactions. The helium gas will actually accelerate as it passes through the tube and it will be stopped and started by the shutter. However, to first order, it is the average mass flow rate of the gas times the tube length that minimizes the diffusion of Flibe through the tube. The gas density at the tube inlet, the shutter open vs. shut time, and the tube length can be adjusted to achieve the desired average mass flow rate and diffusion stopping capability.

It is important that the gas motion not be stopped for a long enough period of time to allow excessive diffusion of Flibe through the length of the tube. The probability of a particle diffusing to a distance x from its starting point in time t is

$$\frac{1}{2\sqrt{\pi Dt}} \exp\left(\frac{-x^2}{4Dt}\right) .$$

Choosing the exponential term equal to 10^{-6} (Argument = -14) and a time of 0.16 s gives

$$x = \sqrt{56Dt} = \sqrt{56(2500 \text{ cm}^2/\text{s})(0.16 \text{ s})} = 150 \text{ cm} .$$

One hundred fifty centimeters is longer than the previously chosen tube length. If the gas is stationary for that long, a higher gas density might be needed to reduce the diffusion coefficient. A factor of 4 increase in density would reduce the diffusion distance by roughly a factor of 2. We now estimate the gas speed that would result from a 4 Torr gas differential pressure. The equation for the frictional force on a gas undergoing laminar flow in a tube from Ref. H-2 is $dF = 8\pi\mu v dx$. The gas will accelerate going through the tube, but a rough idea of a typical speed in the tube can be attained by assuming constant velocity. Then the speed is given by

$$v = \frac{F}{8\pi\mu x} = \frac{P\pi r^2}{8\pi\mu x} = \frac{4 \text{ Torr}(0.01 \text{ m})^2}{8(3.9 \times 10^{-5} \text{ kg/m} \cdot \text{s})(1 \text{ m})} \frac{10^5 \text{ Pa}}{760 \text{ Torr}} = 170 \text{ m/s} .$$

The shutter could remain shut, except for the time required for the Flibe vapor to be removed from the tube, which would be about $0.75 \text{ m}/170 \text{ m/s} = 5 \text{ ms}$. The average gas mass flow rate would be

$$\dot{m} = \rho A x / t \approx \frac{PM}{RT} \pi r^2 \frac{x}{t} = \frac{4 \text{ Torr}(0.004 \text{ kg/mole})}{8.3 \text{ J/mole} \cdot \text{K}(800 \text{ K})} \frac{10^5 \text{ Pa}}{760 \text{ Torr}} (3.14)(0.01 \text{ m})^2 \frac{0.75 \text{ m}}{0.165 \text{ s}} = 0.4 \text{ mg/s}$$

An advantage of this approach is that very little Flibe vapor will even enter the tube while the shutter is open. Returning to the steady state equation

$$c = c_0 \exp \left[- \left(\frac{v}{D} \right) x \right]$$

we can roughly estimate the penetration distance of Flibe vapor from the chamber as

$$x \approx \frac{D}{v} = \frac{625 \text{ cm}^2/\text{s}}{17,000 \text{ cm/s}} = 0.04 \text{ cm} .$$

This is an overestimate since Flibe vapor is being pushed away from the opening by the helium flowing out. More detailed calculations will probably reduce the required gas flow below the 0.4 mg/s just estimated.

If the gas pressure in the target-tracking region is equal to the pressure in the tube, it could significantly effect the target trajectory. For example, the mass of 4 Torr of gas in a 3 m distance of the target's path at room temperature is

$$m = \rho A x = \frac{PM}{RT} \pi r^2 x = \frac{4 \text{ Torr}(0.004 \text{ kg/mole})}{8.3 \text{ J/kg} \cdot \text{K}(273 \text{ K})} \frac{10^5 \text{ Pa}}{760 \text{ Torr}} (3.14)(0.006 \text{ m})^2 (3 \text{ m}) = 0.3 \text{ mg} .$$

If a 2 g target has to travel 5 m after passing through this gas, the change in position would be approximately

$$\Delta x = \frac{\Delta v}{v_0} R = \frac{m}{m_t} R = \frac{0.3 \text{ mg}}{2 \text{ g}} 5 \text{ m} = 0.75 \text{ mm} \quad .$$

This displacement is about equal to the maximum axial beam-target position error, but can readily be compensated for. The gas in the tube will also affect the target trajectory. The gas in the tube is less dense but the coefficient of drag could be greater due to confinement of gas in the tube. We could also reduce the pressure between the gun and the gas diffusion tube with shutters and differential pumping if desired.

In conclusion, rough calculations indicate that the Flibe vapor can be prevented from entering the target-tracking region with a shutter and opposing helium gas flow. The opposing flow of less than 1 mg/s is much less than the amount of helium produced in the chamber from fusion reactions. This mass flow rate includes an increase in average gas density that may be necessary to make up for the start and stop nature of a gas flow with shutters. The most likely solution is to have the shutter shut most of the time and open just long enough to allow the target to pass through and clear the Flibe vapor between each shot. The helium should not excessively affect target position accuracy.

References

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Appendix I: Flibe Droplet Effect on Accuracy and Ignition

As indicated in Fig. I-1, a target of mass m_t , length L , and radius r_t is incident on a droplet of mass m_i , with impact parameter r_i at distance d from the chamber center. What is the effect of the impact on the target trajectory and target tumble? We assume that the droplets initial velocity is negligible, its mass is much less than the target mass, and that it sticks to the target. To estimate the target's moment of inertia, we'll assume a uniform mass density. This will probably underestimate the target's moment of inertia because much of the mass is actually located near the target surface. After considerable integration and algebra, we calculate the moment of inertia about a line perpendicular to the target's axis and passing through its center of mass to be

$$I \approx m_t \left(\frac{r_t^2}{4} + \frac{L^2}{12} \right) \approx 2 \text{ g} \left(\frac{(0.6 \text{ cm})^2}{4} + \frac{(2 \text{ cm})^2}{12} \right) = 0.85 \text{ gcm}^2 \quad .$$

The 2 g assumed for target mass includes 0.6 g for the target plus 1.4 g for the mass of outer support structure.

Fig. I-1. A target impacts a droplet at distance R_c from the chamber center.

We'll first consider the effect of the collision on the target center of mass displacement. If the collision takes place a distance R_c from the chamber center, the change in target position at the time it would have been at the chamber center is given by

$$\Delta R = \frac{\Delta v}{v_0} R_c = \frac{m_i}{m_t} R_c \quad .$$

Consider the example of a 2 g target colliding with a droplet 3 m from the chamber center. What is the droplet mass will cause a 1 mm error in final axial target position?

$$m_i = \frac{\Delta R}{R_c} m_t = \frac{1 \text{ mm}}{3 \text{ m}} 2 \text{ g} = 0.7 \text{ mg} \quad .$$

This mass corresponds to a 0.44 mm radius drop of Flibe with 2 g/cm³ density.

The closer to target center, the larger the mass of the droplet could be. If we measure the target's position a small distance R_m from the chamber center, the final target position could be better predicted. If we assumed an unchanged target velocity, but updated the target position with the measurement, our error would be reduced equivalent to replacing R_c with R_m .

If the droplets are much smaller than 0.7 mg and there are many in a target path, we can calculate the maximum droplet density allowed. In a 3 m radius chamber with uniform droplet density, the average droplet would hit the target 1.5 m away. Using this value for R_c gives $m_i = 1.4 \text{ mg}$. The maximum droplet density would then be given by

$$\rho = \frac{m_i}{V} = \frac{m_i}{\pi r_i^2 R_c} = \frac{1.4 \times 10^{-6} \text{ kg}}{3.14 (6 \times 10^{-3} \text{ m})^2 3 \text{ m}} = 4.1 \times 10^{-3} \text{ kg/m}^3 \quad .$$

We now consider the effect of colliding with a small droplet on target tumble. Conservation of angular momentum L gives $L_{t0} + L_{i0} = L_{tf} + L_{if}$. In the reference frame of the center of mass of the target assuming the target is not initially spinning and recognizing the moment of inertia of the target I_t is much greater than the moment of inertia of the droplet I_i we get

$$0 + m_i r_i v \approx I_t \omega_f + 0 \Rightarrow$$

$$m_i = \frac{I_t}{r_i v} \omega_f = \frac{I_t}{r_i v} \frac{\theta_f}{\Delta t} = \frac{I_t}{r_i v} \frac{\theta_f v}{R_c} = \frac{I_t}{r_i} \frac{\theta_f}{R_c} = \frac{I_t}{r_i} \frac{\varepsilon_{y\theta}}{R_c (L/2)}$$

where ω is angular velocity, θ is the target tumble angle, Δt is the time for the target to pass from the point of collision to the reaction chamber center, and $\varepsilon_{y\theta}$ is the error in final target end position due to tumble. A typical example would be

$$m_i = \frac{2 I_t}{r_i} \frac{\varepsilon_{y\theta}}{R_c L} \approx \frac{2 (0.85 \text{ gcm}^2) (0.02 \text{ cm})}{0.6 \text{ cm} (300 \text{ cm}) (1 \text{ cm})} = 0.2 \text{ mg}$$

which is more restrictive than the linear momentum restriction. Much of this difference is due to the lower target precision required in the axial direction compared to the perpendicular directions.

If the target is spin stabilized, the total tumble angle will be reduced. As indicated in Fig. I-2, the target spins about axis x_3 with angular velocity ω_3 , and tumbles around axis x_1 with angular velocity ω_1 . The net result is a precession with angular velocity ω_{pr} , half angle ϕ and maximum angle θ . Reference I-1 shows that for a rigid body

$$\omega_3 = \omega_1 \frac{I_1}{I_3} \cot \phi \quad .$$

For small ϕ this gives

$$\omega_3 \approx \omega_1 \frac{I_1}{I_3 \phi} = \frac{r_i v m_i}{I_1} \frac{I_1}{I_3} \frac{L/2}{\epsilon_{y\theta}} = \frac{r v m_i L}{2 I_3 \epsilon_{y\theta}} \quad .$$

Assuming the same mass and dimensions used above to calculate I_1 ,

$$I_3 \approx \frac{m_i r_i^2}{2} \approx \frac{2 \text{ g}(0.6 \text{ cm})^2}{2} = 0.36 \text{ gcm}^2 \quad .$$

We can set $\phi = \theta_{\max}/2 = \epsilon_{y\theta}/L$ to determine the spin rate ω_3 required. Assuming a velocity of 200 m/s for the previous example suggests a spin rate of

$$\omega_3 \approx \frac{r_i v m_i L}{2 I_3 \epsilon_{y\theta}} = \frac{0.6 \text{ cm}(20,000 \text{ cm/s})(0.0002 \text{ g})(1 \text{ cm})}{2(0.36 \text{ gcm}^2)(0.02 \text{ cm})} = 170 \text{ rad/s} = 26 \text{ rev/s} \quad .$$

A 200 to 300 rev/s spin rate is expected to be achievable without causing excessive stress in the DT. Therefore, an order of magnitude higher mass droplet (2 mg) is expected to be acceptable without causing excessive tumble of a 2 g spinning target moving at 200 m/s. The maximum droplet mass, in this case, is proportional to the target mass and inversely proportional to the target speed.

Fig. I-2. A spinning target precesses as it travels through the injection path.

I.1. Effect on Target Performance

Extra material on the end of a target will absorb some of the beam energy. Since it is deposited on one end of the target, it will cause axial (polar angle) asymmetry in the target illumination. If not uniformly distributed on the target end, the material will cause additional asymmetries. If the density times thickness of the material is much less than the ion beam range for the target which is about 0.035 g/cm^2 , the target should still ignite and burn to full yield (Ref. I-2). We don't know how much material is too much in this case and can't calculate it with the current 2-D Lasnex code being used. The surface area of the target end is about 1 cm^2 . If about 1 mg of droplet material were uniformly added to the end of the target as suggested by the previous calculations, this would represent about 3% of the ion beam range. If not uniformly distributed, then the beam absorption in the material would be locally higher. One milligram might be the correct order of magnitude of material acceptable for this effect, but further calculations are necessary to verify this.

I.2. Summary and Conclusions

We calculated the approximate effect of inelastic collisions with small droplets on target trajectory as well as the effect on subsequent position prediction accuracy. A 1 mm axial position prediction error results from a collision of a 2 g target with a 0.7 mg droplet 3 m from chamber center. We could reduce this error if target position measurements are taken within the chamber.

Assuming uniform mass density, we calculated the target's moment of inertia. We then calculated the target tumble resulting from the collision of a droplet with a target. A 0.2 mm position error of the target end position results from tumble when a 2 g target strikes a 0.2 mg droplet with 0.6 cm impact parameter 3 m from the chamber center. This error can be reduced by spinning the target, but target spin rate is limited by rotation induced fuel stress.

As a rule of thumb, assuming few drops hit each target, the total mass of droplets in the path of a spinning target should be less (preferably much less) than 0.05% of the target mass (1 mg for a 2 g target).

This has been an abbreviated study of effects of droplets in the beam path. A more detailed study would be warranted if we find the actual droplets in the beam path are roughly the maximum size indicated here. Target performance degradation caused by excess droplet material in the beam path should also be examined in much greater detail. This would include material stuck to the target and material located between the target and the beam port.

I.3. References

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Appendix J: Laser Beam Steering

We now consider whether a commercially available dynamic mirror mount can handle the needs of a direct drive IFE plant.

$$\text{arcsec} \equiv 4.8481368 \cdot 10^{-6} \cdot \text{rad}$$

$$\mu\text{sec} \equiv 10^{-6} \text{ sec}$$

$$\mu\text{m} \equiv 10^{-6} \text{ m}$$

$$R_{\text{mirror}} := 30 \cdot \text{m}$$

The distance of the mirror from tank center (From Sombrero Study).

$$f_{\text{shot}} := 5 \cdot \text{Hz}$$

The shot frequency

$$t_{\text{shot}} := \frac{1}{f_{\text{shot}}}$$

The time between shots

$$S_{\text{target_placement}} := 5 \cdot \text{mm}$$

The radius of the spherical volume into which the injector can place targets with high probability. This shall be referred to as the PSV, placement spherical volume.

$$\alpha_{\text{mirror}} := 2.1 \cdot \frac{\text{rad}}{\text{sec}^2}$$

The maximum acceleration for the Aerotech AOM360D-20 mirror mount in standard configuration. (Motors with about 2 times the standard torque are available.)

The maximum velocity for the Aerotech AOM360D-20 mirror mount in standard configuration.

$$\omega_{\text{mirror}} := 1.05 \cdot \frac{\text{rad}}{\text{sec}}$$

If we start at one side of the PSV with zero velocity and have to hit a target on the other side of the PSV with the mirror at zero velocity, can we do it?

Try an acceleration profile that is symmetric, accelerate at maximum for half of the time and then decelerate at maximum for the last half.

$$\text{Half_time} := \sqrt{\frac{2 \cdot S_{\text{target_placement}}}{\alpha_{\text{mirror}} \cdot R_{\text{mirror}}}}$$

$$\text{Time_to_move_mirror} := 2 \cdot \text{Half_time}$$

$$\text{Time_to_move_mirror} = 0.025 \cdot \text{sec}$$

$$(\text{Time_to_move_mirror})^{-1} = 39.686 \cdot \text{Hz}$$

Thus, this mirror mount can easily keep up with the IFE shot rate of 5 Hz.

$$\text{Max_Vel}_{\text{mirror}} := \alpha_{\text{mirror}} \cdot \text{Half_time}$$

$$\text{Max_Vel}_{\text{mirror}} = 0.026 \cdot \frac{\text{rad}}{\text{sec}}$$

This check okay, the mirror mount velocity maximum 1 rad/s has not been exceeded.

However there is a more stringent time limitation (requirement), the position of the target will not be known until the target has passed the final detector of the tracking system.

This will reduce the time available to move the mirrors in the following way.

$$v_{\text{capsule}} := 500 \cdot \frac{\text{m}}{\text{sec}}$$

This is about the fastest one would want to inject in a Sombrero like chamber (i.e. one with 0.5 Torr xenon in it). Heating by gas is substantial at this point and will be worse at higher velocities.

$$R_{\text{track_final}} := 7.5 \cdot \text{m}$$

This is the closest final detector position. It corresponds to the outer radius of the Sombrero chamber. This is inside of the shielding. Likely one would want to move it

outside of the shielding to about 12 meters, but this is a conservative number to use for determining mirror mount requirements.

$$t_{\text{path_known}} := \frac{R_{\text{track_final}}}{v_{\text{capsule}}}$$

$$t_{\text{path_known}} = 0.015 \cdot \text{sec}$$

Thus, the old mirror moving scheme is not fast enough.

However there is plenty of time (200 ms – 15 ms = 185 ms) to preposition the mirror at the center of the PSV. This will cut in half the distance the mirror must be moved, once the target position is known.

$$\text{Half_time_2} := \sqrt{\frac{2 \cdot \left(\frac{S_{\text{target_placement}}}{2} \right)}{\alpha_{\text{mirror}} \cdot R_{\text{mirror}}}}$$

$$\text{Time_to_move_mirror_2} := 2 \cdot \text{Half_time_2}$$

$$\text{Time_to_move_mirror_2} = 0.018 \cdot \text{sec}$$

This is still not fast enough. However, about twice the angular acceleration is available as a special on the mirror mount. We will find the required acceleration.

$$\alpha_{\text{guess}} := \alpha_{\text{mirror}}$$

Given

$$\frac{t_{\text{path_known}}}{2} = \sqrt{\frac{2 \cdot \left(\frac{S_{\text{target_placement}}}{2} \right)}{\alpha_{\text{guess}} \cdot R_{\text{mirror}}}}$$

$$\alpha_{\text{need}} := \text{find}(\alpha_{\text{guess}})$$

$$\alpha_{\text{need}} = 2.963 \cdot \frac{\text{rad}}{\text{sec}^2}$$

$$\text{Max_Vel_2}_{\text{mirror}} := \alpha_{\text{need}} \cdot \frac{t_{\text{path_known}}}{2}$$

$$\text{Max_Vel_2}_{\text{mirror}} = 0.022 \cdot \frac{\text{rad}}{\text{sec}}$$

This check okay, the mirror mount velocity maximum of 1 rad/s has not been exceeded.

So the mirrors can be moved fast enough.

Can the mirrors be moved accurately enough?

$$\text{Miss_Distance} := 20 \cdot 10^{-6} \cdot \text{m}$$

The laser beam center must hit the target within 20 μm of target center

$$\text{Tracking_Angle} := \frac{\text{Miss_Distance}}{R_{\text{mirror}}}$$

This is the maximum angular misalignment allowed.

$$\text{Tracking_Angle} = 6.667 \cdot 10^{-7} \cdot \text{rad}$$

$$\text{Tracking_Angle} = 3.82 \cdot 10^{-5} \cdot \text{deg}$$

$$\frac{\text{Miss_Distance}}{7.5 \cdot \text{m}} = 0.55 \cdot \text{arcsec}$$

$$\text{Tracking_Angle} = 0.138 \cdot \text{arcsec}$$

The Aerotech AOM360D-20 mirror mount is available with enhanced transducers of

Resolution of 0.1 arcs

Accuracy of 2.0 arcs

Bidirectional repeatability of "1 count", which probably means equal to the resolution.

or

Resolution of 0.36 arcs

Accuracy of 1.0 arcs

Bidirectional repeatability of "1 count", which probably means equal to the resolution.

Other specs of interest

A wobble on Azimuth and Elevation of 1 arc s is available.

Orthogonality of 5 arcs is available.

With the new tracking accuracy required (20 vs. 200 μm), the spec's don't quite make it in a straight forward mode of operation (use accuracy only).

The resolution is high enough to work. If repeatability is at the resolution level, one could map the response of the mount. This would allow aiming at the target accurately enough if the building, chamber, and mount are stable over long time scales.

I presume that wobble refers to in motion, but the above calculations show that the mirror can be motionless at shot time, so this spec should not come into play.

Another possibility is that the encoder that is measuring the motion could be configured for higher accuracy if its maximum range is restricted from the current 360°.

The encoder only needs a range of

$$\frac{2 \cdot S_{\text{target_placement}}}{R_{\text{mirror}}} = 68.755 \cdot \text{arcsec} \quad .$$

The dynamic range required of the encoder is small.

$$\frac{2 \cdot S_{\text{target_placement}}}{\text{Miss_Distance}} = 500 \quad .$$

A nine or ten bit encoder will do; 1 part in 512 or 1024.

Ten bit A/Ds are available with sampling rate of 20 MHz, 50 ns; Texas Instruments TLC876CDW can convert A/D (10 bits) at 20,000,000 samples per second. Programmable Logic arrays are available with 5 ns propagation delay and operate at 182 MHz; Vantis MACH111-5JC.

These times (50 ns) are short compared to

$$t_{\text{path_known}} = 0.015 \cdot \text{sec} \quad .$$

This is good. Most of this time can be spent moving the mirror as opposed to figuring out where to move it.

The Aerotech motor controller (UNIDEX 511 Series Programmable Controller) uses a 40 MHz DSP. In can, update the position of up to 4 axis using PID with feed forward in under 250 μ s.

This means that the mirror steering can be corrected

$$\frac{t_{\text{path_known}}}{250 \mu\text{sec}} = 60$$

times during the course of aiming the mirror for one shot.

For timing, in 1985 Fairchild made an up/down binary counter with preset and ripple clock (module 16 cascable), the 74F191, that had a typical counting frequency of 125 MHz and a propagation delays from 4 to 11 ns typical (depending on operation). It was made from an "Advanced Schottky TTL" technology.

Texas Instruments now markets logic chips with the AVC technology (Advanced Very-low-voltage CMOS) with less than 2 ns propagation delay. Whether they make counters with this yet, I do not know.

Need timing to better than

$$\frac{\text{Miss_Distance}}{v_{\text{capsule}}} = 0.04 \cdot \mu\text{sec}$$

to hit target. With counter running at 125 MHz and throwing in a factor 3 for three bit errors at each of three timing stations (up, down, trigger), one could achieve, using 1985 era technology, a timing accuracy of no worse than,

$$(125 \cdot 10^6 \text{ Hz})^{-1} \cdot 3 = 0.024 \cdot \mu\text{sec}$$

This is sufficient to hit the target.

Noting that the Aerotech mirror mount mentioned above was a gimbaled mirror mount, which is not really close to the GIMM or GILMM concepts in the Sombrero report, I will now look at modeling something closer to GIMM (Grazing Incidence Metal Mirror). In Sombrero, the GIMM was 4 m by 1 m.

Let us imagine this a steel plate 1 cm thick (or a honeycomb structure with the same mass but thicker). Assume that it is suspended on a pivot that lets it tip and tilt about its center. Assume that for linear brushless magnetic induction motors are installed, one at each corner, to move the GIMM.

$$L_{\text{GIMM}} := 4 \cdot \text{m}$$

$$W_{\text{GIMM}} := 1 \cdot \text{m}$$

$$H_{\text{GIMM}} := 1 \text{ cm}$$

$$\text{Vol}_{\text{GIMM}} := L_{\text{GIMM}} W_{\text{GIMM}} H_{\text{GIMM}}$$

$$\text{Vol}_{\text{GIMM}} = 0.04 \cdot \text{m}^3$$

$$m_{\text{GIMM}} := 8 \frac{\text{gm}}{\text{cm}^3} \cdot \text{Vol}_{\text{GIMM}}$$

This uses about the density of stainless steel.

$$m_{\text{GIMM}} = 320 \cdot \text{kg}$$

From torque = moment of inertia * angular acceleration, we have force normal = angular acceleration * moment of inertia/radius of force normal which in the case of a thin rectangular block pivoting about its center around a principle axis gives force normal = angular acceleration * (1/12) * mass * (length of GIMM side)²/radius of normal force, where length of GIMM side is length of block, perpendicular to the principle axis that block is rotated about.

Since linear motors are installed at corners of GIMM, length of GIMM side = 2 * radius of force normal. Therefore, force normal = angular acceleration * (1/6) * mass * length of GIMM side.

$$F_{\text{Needed_of_motor}} := \alpha_{\text{need}} \cdot \frac{1}{6} m_{\text{GIMM}} L_{\text{GIMM}} \frac{1}{2}$$

where last factor of 2 comes from the fact that there are 2 motors per axis of motion (tip and tilt).

$$F_{\text{Needed_of_motor}} = 316.049 \cdot \text{newton}$$

Aerotech makes linear motors capable of as much as 1186 N continuous with cooling, 742 N continuous without cooling, and 4744 N peak (model BLMX-502-C). The size of this motor is 502 mm (forcer length) x 50.8 mm x 152 mm (note magnet track will be larger than forcer by at least length of travel required which in this can is very small).

Perhaps better match is the BLMH-202 which can exert 356 N continuous and 1420 N peak. The size of this model is 202 mm forcer length x 50.8 mm x 114 mm (note magnet track will be larger than forcer by at least length of travel required which in this case is very small).

Next, the position indication will be looked at. If a linear position encoder is installed on the linear motor it will need a travel length of

$$\left(\frac{W_{\text{GIMM}}}{2} \right) \cdot \left(\frac{2 \cdot S_{\text{target_placement}}}{R_{\text{mirror}}} \right) = 166.667 \cdot \mu\text{m}$$

It needs a dynamic range of

$$\frac{2 \cdot S_{\text{target_placement}}}{\text{Miss_Distance}} = 500$$

or a non-linearity of no more than

$$\left(\frac{2 \cdot S_{\text{target_placement}}}{\text{Miss_Distance}} \right)^{-1} = 0.2 \cdot \%$$

or an accuracy of

$$\left(\frac{W_{\text{GIMM}}}{2} \right) \cdot \left(\frac{2 \cdot S_{\text{target_placement}}}{R_{\text{mirror}}} \right) \cdot \left(\frac{2 \cdot S_{\text{target_placement}}}{\text{Miss_Distance}} \right)^{-1} = 0.333 \cdot \mu\text{m}$$

Update rate should be significantly faster than the update rate of the linear motor controller, for the Aerotech UNIDEX 511,

$$10 \cdot (250 \mu\text{sec})^{-1} = 4 \cdot 10^4 \cdot \text{Hz}$$

Suitable transducers are:

Heidenhain LIP101 or LIP501,

Opti-Cal ZPE-100R (8 MHz, 0.01 μm repeatability, 0.02 μm accuracy, 300 mm travel, max velocity at least 500 mm/s.

Both work by shining laser off of ruled gratings and detecting intensity variations which are counted and interpolated.

Also the Mechanical Technologies Incorporated — Microtrack 7000 with MT-100-20 head: 20 kHz response, 500 μm range, 0.013 μm resolution, 0.1% of range accuracy, 0.1% of range linearity. Principle: laser triangulation.

Appendix K: Reflectance Measurements of Gold/Kapton Films and Spectrally Selective Coatings

K.1. Background

A thin foil film was obtained from Schafer Corporation. The film consists of a gold and Kapton bilayer repeated 10 times for a total thickness of 1.39 microns. Each Kapton film is nominally 0.1 microns thick and each gold film is nominally 0.06 microns thick. The film was subjected to reflectivity measurements in the infrared. When the weighted data (see below) are compared to the black body radiation spectrum at 1758 K (i.e., the SOMBRERO chamber), the overall energy reflectance is estimated to be about 29% (Fig. K-1).

Fig. K-1. Reflection for Gold/Kapton capsule.

K.2. Discussion of Results (Rich Stephens, GA)

Attached is an estimate of the average hemispherical reflectance of the 10 repeat Au-Kapton film made by Schafer for NRL. This spectrum is for the side with the gold layer on top; the opposite side, with the Kapton on top was also measured, and has much lower reflectance. The films were prepared by floating on water and lifting them off onto a microscope slide. When completely dry, the films had only a small number of fine wrinkles on the surface; they did not interfere with measurements. Reflectance measure-

ments were made by J.A. Woollam Co. at four angles of incidence (30, 45, 60, and 75 degrees), and in the infrared also for two different polarizations (s and p). The data ranges all the way through the visible out to 40 microns. The hemispherical reflectance calculated from that data is an average weighted by the solid angle in each of the bands (0-37.5°, 37.5-52.5°, 52.5-67.5°, and 67.5-90°) around the angles used for measurement.

The reflectance of this film is much lower than that of bulk gold. Part of that is because the top gold layer is so thin that it transmits light into the multilayer stack; at shorter wavelengths, you can see the resulting oscillations in the reflectance. The other part of that is because the vapor deposited gold layer does not have the optical properties of bulk gold. At best, the deposit is somewhat less dense than the bulk, and at worst, it could have been deposited as isolated, ellipsoidal particles. In any case, the spectroscopist at J.A. Woollam Co. (Ron Synowicki) could not fit his results to literature values of the materials involved. I calculate that this type of layer will need a much thicker outside gold layer (near 0.2 micron, depending on how good the deposition technique is) to boost reflectance to 98%.

K.3. Discussion of Spectrally Selective Coatings (Ron Petzoldt, UC Berkeley)

We now answer the question of what wavelengths of light do we need to shield our targets from. The peak radiation occurs for $\lambda(\mu\text{m}) = 2900/T$. 75% of the energy is at longer wavelengths. 90% of the energy is at wavelengths longer than 3/4 of the peak wavelength and 99% is at wavelengths longer than 1/2 of the peak wavelength. At 1000 K, 90% of the energy is longer wavelength than 2.2 μm and at 2000 K 90% of the energy is longer than 1.1 μm . A coating which would reflect wavelengths longer than 1 μm and transmit visible wavelengths (0.4–0.7 μm) would serve our needs. Noble metals such as silver (or copper or gold) on a surface provide high reflectance and low thermal emittance

in thickness greater than about 20 nm (>90% for a 40 nm thickness of silver). However, visible transmittance (e.g. for characterization) is poor. Metallic coatings alone may suffice for IFE if we don't have to look inside the targets. The visible light transmittance can be enhanced by a layer of high refractive index dielectric (such as Bi_2O_3 , In_2O_3 , SnO_2 , TiO_2 , ZnO , and ZnS) on both sides of the metal. As illustrated in Fig. K-2, sandwiching a layer of silver between layers of TiO_2 can provide 80% transmission of visible light, with about 95% reflectance of infrared.

Fig. K-2. Spectral normal transmittance and near normal reflectance for $\text{TiO}_2/\text{Ag}/\text{TiO}_2$ based coatings on glass. Coating thicknesses are shown. Reproduced from C.G. Granqvist, *Spectrally Selective Surfaces for Heating and Cooling Applications*, (SPIE Optical Engineering Press, Bellingham, Washington, 1989).

Doped oxide semiconductor coatings such as $\text{SnO}_2:\text{Sb}$, $\text{In}_2\text{O}_3:\text{Sn}$, and $\text{ZnO}:\text{Al}$ are also used for energy efficient windows, but they don't reflect infrared as effectively, especially short wavelength infrared between 1 and 2 microns. This is good for windows in cold climates, but is not optimum for our needs. The process to apply this coating has not been determined. Some possibilities are sputtering, vacuum evaporation, and chemical vapor deposition.

Appendix L: Induction Accelerator Plan — D3 Enterprises*

L.1. Introduction

The purpose of this study is to provide initial input on the feasibility of an electromagnetic IFE target injector and provide a first order cost estimate for a construction program. This study was performed by D3 Enterprises under contract to General Atomics.

The study was performed in five steps:

1. The requirements for the injector were reviewed.
2. The potential electromagnetic injector concepts were reviewed.
3. An approach was selected.
4. A design was sufficiently defined to afford a rough order of magnitude (ROM) cost.
5. The study was documented in this report.

This report will discuss the factors leading to the selection of a normal conducting induction gun concept, and the design that followed. A three-year program was envisioned that would result in a working experimental injector, capable of multiple shots. The program would be in two phases. The first phase would perform the design and analysis to construct a short demonstration injector. This would be used to confirm the analytical model and develop the electronics and coil design. In the second phase, a full size, experimental injector would be constructed and tested. This prototype should be capable of accelerating sample targets to 400 meters/second at accelerations up to 5000 G. The total combined estimated cost of Phase I and II is approximately \$3.3 million.

L.2. Injection System Requirements

The experimental injector must be capable of accelerating 1 gram cryogenic targets to velocities of 400 m/s. It must have multi-shot capability and be ultimately capable of handling cryogenic targets at temperatures of 18-19K. Table L-1 summarizes the requirements provided by General Atomics for an electromagnetic experimental injector.

* Approved by D3 Enterprises, 7710 Balboa Ave. 228D, San Diego, California 92111.

Table L-1
Injector System Requirements

Parameter	Requirement	Units / Notes
Target Speed	400	m/s
Acceleration	5000	G's
Repetition Rate	Multi-Shot Burst	(Upgradable to Cryogenic)
Target Mass	<1	g
Target Diameter	<12	mm
Target Temperature Range	18-19	K (Initially at R.T.)
Target Spin	300	RPS
Target Exit Accuracy	0.3	mradians
Cryogenic Fuel Temperature Rise	0.5	K
Availability	Several Shots per Hour	

L.3. Electromagnetic Injection Options

General Atomics personnel performed a brief study to identify options for accelerating typical targets to speeds in the range of 400 m/s. The study (Ref. L-1) suggested two basic approaches, with variations.

The first approach was based on the ferromagnetic effect. The target is propelled by attracting either a piece of ferromagnetic material, or a permanent magnet, through a barrel, by a series of solenoid coils. As the magnetic material passes a coil, that coil is turned off, so as not to slow the projectile.

The second approach was similar to a linear induction motor. In this case the target is repelled through the barrel by a series of primary coils which induce a current into a conductive ring, or armature, attached to the target. The primary coils are timed to energize as the projectile passes the center of each coil. Therefore, each coil adds energy to the secondary coil attached to the target.

The following sections discuss each of the suggested options and make a concept selection.

L.3.1. Ferromagnetic

In the ferromagnetic accelerator, the target is "dragged" down the barrel by the attractive force of circumferential barrel solenoid magnets. The forces calculated in Ref. L-1 were confirmed by creating a simple spreadsheet model which calculated the field gradient between the front and back of an iron cylinder, at various locations along the barrel. There is very little heating of the iron during the acceleration, due to its poor conductivity. Therefore the temperature rise is very small. The major disadvantage of the

attractive systems is stability. While the iron is attracted along the bore by the series of solenoids, there is a large destabilizing force that wants to displace the projectile radially. The more the radial displacement, the greater the force. This will result in frictional heating in the barrel, as only mechanical contact can restrain the projectile. This is likely to contribute to targeting errors. A barrel wear pattern could emerge that might quickly degrade the injector and its targeting precision.

L.3.2. High Temperature Superconducting

The high temperature superconducting (HTSC) accelerator uses a cylindrical HTSC ring, attached to the target, to accelerate the target repulsively along the barrel. Current is induced into the secondary ring from circumferential primary coils surrounding the barrel. This is similar to an induction motor, or more specifically a linear induction motor. The current in each primary coil is phased, or timed, to induce a current in the secondary HTSC ring as it passes. This induced current creates a magnetic field opposite that of the primary. This, in turn, creates a repelling force between the two coils. If the primary coil currents are timed properly, the target can be continuously accelerated through the barrel. The fundamental advantage of this system is its stability. Unlike the attractive system, the repulsive system is naturally stable, with the target automatically centered throughout the barrel. There is no need for extensive mechanical contact, and therefore improved repeatability.

It has been proposed that the HTSC coil will not generate heat because it is superconducting. This may be true for pure HTSC material under dc conditions. It is likely there will be some normal conducting stabilizer required to maintain stability of the HTSC. Also, the high frequencies involved may create additional heating.

Reference L-1 concluded that HTSC materials are not currently suitable for injectors having velocities in the hundreds of m/s. These calculations were confirmed, and even optimistically, it will be several years before this is at all feasible.

L.3.3. Normal Conduction

A normal conducting secondary coil in a repulsive induction accelerator works similarly to an HTSC secondary. An opposite field is induced in the secondary which repels the target along the barrel. Materials such as copper and aluminum are candidates for a normal conducting secondary coil. Reference L-1. discounted normal conducting coils due to the heat generated during acceleration. We found, that for velocities of 400 m/s and accelerations of 5000 Gs, it is possible to achieve acceptable temperature rises in the range of 10 K or less, assuming barrel magnetic fields on the order of 1 Tesla.

Consider the example shown in Fig. L-1.

Fig. L-1. Armature secondary model.

Table L-2 shows a comparison of two different armature materials (copper and aluminum), at three different temperatures; 300 K, 100 K, and 20 K. In all cases the temperature rise is less than 10 K for accelerating fields of 1 Tesla. It is entirely reasonable to obtain fields of one or more Tesla using copper primary coils in the pulsed mode. This table also supports the possibility of first testing a system at room temperature, and then, later operating the same system at the required cryogenic temperatures. The temperature rise appears to be relatively insensitive to temperature.

Table L-2
Comparison of Temperature Rise for Aluminum and Copper Armatures
in 1 Tesla Field

	Cu-300 K	Cu-100 K	Cu-20 K	Al-300 K	Al-100 K	Al-20 K	Units
Armature							
Inner Radius	0.0025	0.0025	0.0025	0.0025	0.0025	0.0025	m
Outer Radius	0.005	0.005	0.005	0.005	0.005	0.005	m
Length	0.02	0.02	0.02	0.02	0.02	0.02	m
Density	8900	8900	8900	2700	2700	2700	kg/m ³
Resistivity	1.6E-08	4.0E-09	1.5E-10	2.7E-08	5.0E-09	2.0E-11	ohm-m
Specific Heat	385	254	7.7	902	481	8.9	j/kg/K
Field	1	1	1	1	1	1	T
Injector Parameters							
Acceleration	5000	5000	5000	5000	5000	5000	g's
Acceleration	49000	49000	49000	49000	49000	49000	m/s/s
Velocity	400	400	400	400	400	400	m/s
Calculated Parameters							
Time	0.00816	0.00816	0.00816	0.00816	0.00816	0.00816	s
Length of Barrel	1.63265	1.63265	1.63265	1.63265	1.63265	1.63265	m
Volume (Armature)	1.2E-06	1.2E-06	1.2E-06	1.2E-06	1.2E-06	1.2E-06	m ³
Mass (Armature)	0.01049	0.01049	0.01049	0.00318	0.00318	0.00318	kg
Mass (Payload)	0.001	0.001	0.001	0.001	0.001	0.001	kg
Mass (Total)	0.01149	0.01149	0.01149	0.00418	0.00418	0.00418	kg
Force	562.768	562.768	562.768	204.862	204.862	204.862	N
Armature Circumference	0.02356	0.02356	0.02356	0.02356	0.02356	0.02356	m
Armature CrossSec Area	0.00005	0.00005	0.00005	0.00005	0.00005	0.00005	m ²
Resistance	7.5E-06	1.9E-06	7.1E-08	1.3E-05	2.4E-06	9.4E-09	ohms
Force/Length	23884.6	23884.6	23884.6	8694.62	8694.62	8694.62	N/m
Current	23884.6	23884.6	23884.6	8694.62	8694.62	8694.62	A
Power	4.3E+03	1.1E+03	4.0E+01	9.5E+02	1.8E+02	7.1E-01	W
Energy	3.5E+01	8.8E+00	3.3E-01	7.8E+00	1.5E+00	5.8E-03	J
Temperature Rise	8.70	3.30	4.08	2.71	0.95	0.21	K

The normal conducting armature also has the benefit that it is self centering in the barrel, just as the HTSC armature. One additional benefit is the low cost. Armatures could be manufactured with any number of low cost processes, unlike the expensive HTSC material armatures.

L.3.4. Concept Selection

For the purpose of this study we selected a normal conducting copper armature. It offers improved stability over the attractive ferromagnetic systems, is low cost, and will not overly heat the target at cryogenic temperatures. Copper was chosen over aluminum because of its improved structural capacity in low resistivity forms. Even though an aluminum armature offers reduced heating, the low resistivity aluminum is extremely soft. Of the three proposed approaches considered, it offers the most promise for a straightforward experimental injector. Table L- 3 summarizes these conclusions.

Table L-3
Comparison of Armature Concepts for Electromagnetic Injector

	Ferromagnetic	HTSC	Normal Conducting
Propulsion	Attractive	Repulsive	Repulsive
Stability	Poor	Good	Good
Available Currently	Yes	No	Yes
Armature Cost	Low	High	Low
Temperature Rise	<10 K	<10 K (?)	<10 K

L.4. Injector Design Concept

In this section we will describe the approach taken to develop a physically realizable concept for an injector meeting the requirements described above and using a normal conducting copper armature. Linear induction motors and induction guns have been developed for a number of applications, from launching payloads to space, to sophisticated weapons of the future. Much of the research in this area was supported with DoD funding during the past two decades. Therefore, there are a number of studies and references available on the subject. References L-2, and L-4 through L-8, are examples where additional data can be obtained.

L.4.1. Design Model

The scope of this brief study required a simple model to gage the required equipment and development costs. For this reason we selected the simple model in Ref. L-2. This

reference provides a set of equations that can be used to define a simple coil gun model using capacitance discharge to energize individual coils. These equations were placed into an Excel spreadsheet, and the variables optimized to achieve the requirements for this injector.

The spreadsheet is shown in Table L-4 with the optimized parameters to achieve 400 m/s. This results in a series of approximately eighty — 2 cm spaced coils. Each of the single turn coils is driven by a 270 microfarad capacitor, charged to 466 volts. This results in about 70,000 amp-turns per coil. These coils can be partitioned to reduce the current requirements. To develop a reasonable cost, each coil was subdivided into six, six-turn spiral coils. This reduces the switched current to less than 2 kA where components are readily available.

Table L-4
Simple Coil Gun Design Model for Injector

Inputs	
Projectile mass, m (kg)	0.006
Inner radius primary coils, Ri (m)	0.01
Coil spacing, w (m)	0.02
Capacitor voltage, V (Volts)	466.88
Peak field, B (Tesla)	2.24
Turns/coil, Nk	36
Initial velocity, v(0) (m/s)	10
Slip velocity, U (m/s)	5
Constants	
Permeability, μ	1.25664×10^{-6}
Efficiency, η	20%
Density, ρ (kg/m ³)	8.90×10^3
Specific heat, χ (j/kg/K)	385
Effective radius, r (m)	0.0175
Inductance function, γ	1.05431203
Solution	
Inductance, L (H)	2.31855×10^{-8}
Acceleration, a (m/s/s)	49000
"g's", A (g)	5000
Velocity, v (m/s)	400
Energy/coil, E (J)	29.4
Temperature rise in coil	2.77×10^{-1}
Capacitance, C (F)	0.000269754
Amp turns, Nk/k (A-T)	71219
Length, s (m)	1.552040816
Effective armature length, Zk (m)	0.079795918
Number of coils, k	78

There is considerable further optimization that can be accomplished on this design. For example, the inductance/capacitance combination at each barrel coil can be optimized to the speed of the target as it passes. While this is more reasonable for efficient energy transfer, it is not considered a major cost driver. The cost is more driven by the total stored energy requirements and number of coils, switches, trigger circuits, etc. Spacing and redistribution of stored energy along the barrel will be necessary in any real injector, but these details will be determined by the detailed analysis in the next development phase.

This design concept can be used at even faster velocities. Accelerations may have to be reduced to avoid heating, but higher velocities, in the range of 1000 m/s are well within the capability of the control system and coil propulsion concept. At 1000 m/s and 5000 gs, the barrel length extends to 10 m. This would drive the program cost significantly, as discussed in the cost section.

L.4.2. Armature Concept

The armature would consist mainly of an annular copper ring and a foam plug, which would insulate and protect the target. The armature can be extruded, and sliced to allow inclusion of small coining marks to interact with barrel tube rifling. This would provide spin with the minimum amount of frictional interaction. One concept for the armature/target assembly is shown in Fig. L-2.

Fig. L-2. Armature/target concept.

L.4.3. Coil Concept

Reasonable partitioning of the model provides a baseline of approximately 480 six-turn spiral coils to accelerate the armature to 400 m/s. These coils can be wound with 0.1 in. insulated square copper wire as shown in Fig. L-3.

Fig. L-3. Single six-turn spiral accelerator coil.

The coils can be assembled on to the barrel in a clocked fashion to allow access to the terminations. When complete, the entire coil set can be impregnated.

L.4.4. Barrel Concept

The barrel itself consists of an injector turret, the barrel with its accelerator coils, and an armature collection system. The injector turret contains several target/armature combinations. Each can be individually injected into the barrel at some initial velocity by a solenoid plunger. After injection, the turret is indexed for the next target. This assembly should be capable of 5–10 shots before reloading. The barrel consists of a bore tube surrounded by the 480 laminated accelerator coils. The exit end of the barrel contains additional coils, which can be timed to slow the armature, thus releasing the target. Additional deflecting coils can shuttle the used armatures into a recycle bin. Supporting structure will be required around the coils to support the electromagnetic loads. Figure L-4 shows the concept schematically.

Fig. L-4. Injector accelerator concept.

L.4.5. Power System

Each coil in the accelerator is driven by a 45-microfarad capacitor charged to approximately 500 volts. A thyristor switch, capable of handling a 2000 amp single half-sine pulse, releases the energy in the capacitor. Each capacitor will have to be recharged

between shots. If we assume the recharge must occur in 100 milliseconds, then 24 kW is required to recharge the 2,400 joules, and the power supply must supply at least twice this amount plus other losses. For costing we will assume 75 kW at 500V.

We considered alternate charging and discharge configurations that conserve on either power supply size or number of capacitors, by using a single capacitor to energize more than one coil. For the purpose of costing we have discarded these additional optimizations due to the potential need to tailor capacitor size and charge voltage progressively to individual coils. To assure adequate resources downstream during detail design, the simple circuit in Fig. L-5 is used for costing the power system.

Fig. L-5. Simplified power system schematic for accelerator coils.

The thyristors must be capable of the 2000 amp half-sine pulse generated by the capacitor/accelerator coil combination. A typical example is the International Rectifier ST230S Series. These thyristors are rated at 230 A continuous and greater than 5000 A for a single 8.3 ms pulse. The repetition rate of the injector should be acceptable at currents less than 2000 A.

The energy storage capacitors are sized at 50 microfarads and 600 volts peak. These capacitors must exhibit extremely low equivalent series resistance (ESR) to handle the large peak currents. One possibility is the non-electrolytic commutation capacitors used in high power switching inverters. These capacitors are specifically designed for high current operation. They are made by GE, Siemens, and a number of other manufacturers.

The charging power supply is available from a number of suppliers. It has been sized at 500 V dc and 75 kW to assure sufficient capacity to recharge the network every 100 ms. Figure L-5 shows a series current limiting resistor to control the charge rate of each capacitor. It may be economical to replace this resistor with a single series boost regulator switching circuit, with diodes replacing the series resistors, as shown in Fig. L-6.

Fig. L-6. Alternate charge pump recharge circuit.

There is some cost reduction in the power supply and the elimination of series resistors. This may be offset by the cost of the inductor, thyristor switch, and catch diodes.

L.4.6. Instrumentation and Control System

The control system is responsible for charging and firing the thyristor switches at the appropriate times to achieve the correct velocity and acceleration profile. When the target is fired, instrumentation is required to measure both the shot parameters, and the welfare of the injector.

L.4.6.1. Controls. The control system consists of a computer to control the operation, sequencing and timing of the accelerator coils, and the firing circuits, which trigger the thyristor switches for each coil. At 400 m/s the minimum timing between coils is about 300 microseconds. This timing can be controlled by conventional digital circuitry integrated to a data acquisition and control computer. Thyristor firing circuits are available at the chip level, and can be mounted on standard boards 5–10 to a board.

L.4.6.2. Instrumentation. Instrumentation is required for monitoring each target's injection and the operation of the injector itself. Standard instrumentation will be used for each of these. Shots can be timed and the velocity measured by conventional laser timing instrumentation placed along a tube used to catch the armature/target and protect personnel.

The injector will have voltage sampling at each coil to validate and record operation. In addition, there will be temperature and strain sensors attached to the coils and the supporting structure.

L.4.7. Issues and Concerns

Within the scope of this study it was impossible to address all the issues required to develop an electromagnetic injector. This section identifies three critical issues, which should receive early priority in any ongoing development process.

L.4.7.1. Coil Gun vs. Traveling Wave Launchers. Pushing the armature along the barrel requires currents in both the primary and secondary coils. For the proposed capacitance discharge, coil gun type injector, the armature sees a relatively constant background field as it is accelerated along the barrel. This may cause the initial current induced in the armature to decay, reducing the force. One solution is to continuously power the coils from a variable frequency power source, so that the local field has a sinusoidal component. As the armature is accelerated, the frequency is increased to maintain an optimum slip speed. A detail model of the injector will have to be developed to determine the optimum match between power system, timing and armature speed. It may be possible to resolve this by altering the timing of the coil currents with respect to the armature position. In this way, a non-uniform field would allow "recharging" of the secondary.

L.4.7.2. Coil Coupling. The present design of the barrel primary coils has an average radius significantly greater than that of the armature. This results in a somewhat poor coefficient of coupling between the primary coils and the armature secondary. It will be necessary to further optimize the coil system to try and improve the coupling coefficient, and therefore increase the efficiency. The limiting factors will be the current densities in the copper coils and the resulting temperature rise.

L.4.7.3. Armature/Target Structure. The interface between the armature and the target will require significant design attention. At 5000 g the forces will become quite significant on the any foam insulating structure. It may be difficult for it to support its own mass, aside from the target mass. This is likely to require a test program to develop the structural and thermo-insulating properties of candidate materials and structures. The armature itself sees predominately a body force, and is therefore not as critical structurally.

L.5. Development Program Concept

The construction of an experimental injector is very much a development project. Costs will be dependent on engineering personnel staffing, schedule, materials and equipment. To determine a realistic cost for the experimental injector, a two phase development program is envisioned. The first phase of this program would determine the detailed analytical basis for the design and proceed to develop a short injector prototype to validate the design concept. In the second phase, the full-size experimental injector would be designed, fabricated, and tested.

L.5.1. Phase I Activities

The following activities are planned for Phase I.

- Analysis with Sandia computer code “Slingshot” or Equivalent
- System design for experimental injector
- Design and build model coil, thyristor switch and trigger hardware
- Test and confirm waveforms
- Replicate coil and electronics — 10 coils
- Test coil group with armature

L.5.1.1. Schedule. Phase I is estimated to take between 9 and 15 months, with a nominal length of 12 months.

L.5.1.2. Staffing.

Management (heads)	0.25
Analysis (heads)	0.5
Design (heads)	0.5
Testing (heads)	0.5
Technician support (heads)	0.5

L.5.1.3. Equipment.**L.5.2. Phase II Activities**

The following activities are planned for Phase II:

- Design and build complete barrel, coils, power system and electronics
- Design and build test facility to maintain vacuum, catch targets and protect personnel
- Test injector and confirm velocity and acceleration for single shots
- Design and build injector/turret assembly
- Test entire system in multi-shot mode

L.5.2.1. Schedule. Phase II is estimated to take between 18 and 30 months, with a nominal length of 24 months.

L.5.2.2. Staffing.

Management (heads)	0.5
Analysis (heads)	0.5
Design (heads)	0.75
Testing (heads)	0.75
Technician support (heads)	1.5

L.5.2.3. Equipment.

Accelerator

- 500 Coils/insulators
- 500 Thyristor switches
- Barrel/support assembly
- Injector /turret assembly

Power System

- 500 Storage capacitors
- 500 Current limiting resistors
- Recharge power supply
- Buswork

Control System

- 500 Thyristor trigger circuits
- Control computer
- Sensors /signal conditioning
- Laser velocity system
- Data acquisition system

Test Facility

- Vacuum system
- Vacuum tube/tank/catcher
- Miscellaneous

L.6. Program Costs

The total program cost for labor and materials was estimated to be slightly over \$3 million including a 20% contingency.

Table L-5 summarizes the data for the two phases. Note, that all cost numbers in the following tables are in FY99 dollars.

Table L-5
Program Cost Summary (\$)

Cost Summary	Phase I	Phase II
Subtotal labor	523,200	1,785,600
Subtotal material	69,200	415,000
Total	592,400	2,200,600
Contingency at 20%	118,480	440,120
Phase total	710,880	2,640,720
Grand total		3,351,600

To understand better the relationship between barrel length and cost consider the following assumptions.

- All Phase I costs remain essentially the same.
- The Phase II material costs would be proportional to the barrel length
- The Phase II Technician costs would be proportional to the barrel length
- All other Phase II labor costs remain the same.

This results in a total program cost growth as depicted in Fig. L-7.

Fig. L-7. Total program cost vs. barrel length.

L.6.1. Phase I Cost Estimate Breakdown

	Heads	Months	Rate (\$)	Cost (\$)
Staffing				
Management	0.25	12	160	76,800
Analysis	0.5	12	140	134,400
Design	0.5	12	115	110,400
Testing	0.5	12	115	110,400
Technician support	0.5	12	95	91,200
Subtotal labor				523,200
Equipment				
Equipment	Qty	Make/Buy	Cost (\$)	Qty Cost (\$)
Accelerator				
Coils/insulators	10	M	20	200
Thyristor switches	10	B	200	2,000
Barrel/support assembly	1	M	1,000	1,000
Injector /turret assembly	N/A			
Power System				
Storage capacitors	10	B	250	2,500
Current limiting resistors	10	B	50	500
Recharge power supply	1	B	40,000	40,000
Buswork	1	M	2,000	2,000
Instrum/Control System				
Thyristor trigger circuits	10	M	100	1,000
Control computer	1	B	5,000	5,000
Sensors/signal conditioning	1	B	5,000	5,000
Laser velocity system	N/A			
Data acquisition system	1	B	10,000	10,000
Test Facility				
Vacuum system	N/A			
Vacuum tube /tank/catcher	N/A			
Miscellaneous	N/A			
Subtotal material				69,200
Total				592,400
Contingency at 20 %				118,480
Grand total				710,880

L.6.2. Phase II Cost Estimate Breakdown

	Heads	Months	Rate (\$)	Cost (\$)
Staffing				
Management	0.5	24	160	307,200
Analysis	0.5	24	140	268,800
Design	0.75	24	115	331,200
Testing	0.75	24	115	331,200
Technician support	1.5	24	95	547,200
Subtotal labor				1,785,600

Equipment	Qty	Make/Buy	Cost (\$)	Qty Cost (\$)
Accelerator				
Coils/insulators	500	M	20	10,000
Thyristor switches	500	B	200	100,000
Barrel/support assembly	1	M	10,000	10,000
Injector /turret assembly	1	M	25,000	25,000
Power System				
Storage capacitors	500	B	250	100,000
Current limiting resistors	500	B	40	20,000
Recharge power supply	0	B	40,000	—
Buswork	1	M	20,000	20,000
Instrum/Control System				
Thyristor trigger circuits	500	M	50	25,000
Control computer	0	B	5,000	—
Sensors/signal conditioning	1	B	25,000	25,000
Laser velocity system	1		10,000	10,000
Data acquisition system	0	B	10,000	—
Test Facility				
Vacuum system	1		10,000	10,000
Vacuum tube /tank/catcher	1		35,000	35,000
Miscellaneous	1		25,000	25,000
Subtotal material				415,000
Total				2,200,600
Contingency at 20%				440,120
Grand total				2,640,720

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Appendix M: Evaluation of Pellet Injector Options

H. Gurol, N.B. Alexander, D.T. Goodin, L. Nguyen, P. Jeter, E. Leung
General Atomics, December 22, 1998

M.1. Introduction

The purpose of this IR&D* was to investigate the options available for injecting both direct and indirect targets into ICF reaction chambers at acceleration levels that are high enough to produce the projected speed requirements of 200–2,000 meters/s. This must be achieved without exceeding the thermal limits of the pellet. The intent is to make recommendations on the best options for reaching these goals. In addition, the selected concepts need to be scalable from an experimental test unit to production injector parameters with a high level of confidence. In the following section, we will discuss the requirements that are projected for both types of targets for an experimental injector, as well as a production unit. This will be followed by a discussion of the available options that have been discussed by others in the past, along with a recommendation of which options merit further study. These options will then be investigated for basic feasibility. Finally, recommendations will be made on which approaches should be analyzed at a greater level of detail.

M.2. IFE Accelerator Requirements

The requirements for pellet injection depend to a large extent on whether the injector will be for a production or experimental facility. Some other factors that drive the high level requirements include: the type of target (direct or indirect), the temperature environment of the reaction chamber, the real estate available for the accelerator, and the accuracy with which the target must exit the injector. The goal will be to ultimately design an injector system that is capable of meeting the future needs of Inertial Fusion Energy (IFE). To this end, we have considered accelerator systems capable of injecting both direct and indirect targets, at speeds that meet the upper end of the required range.

Table M–1 summarizes the requirements that are necessary to achieve target injection into the reaction chamber. Two columns are shown; one is for a production fusion machine injector, the other for an experimental facility. In previous studies, it was assumed that targets could be cooled to 4 K and then warm to ~18.5 K during their transit to target chamber center. It has since been found that a temperature rise of more than ~0.5 K results in a DT layer that is unacceptable for implosion. One solution to this

*IR&D work completed prior to contract and included as useful background information and not utilized to meet any project milestones.

Table M-1
Requirements for Production and Experimental Cryogenic Target Injectors

Parameter	Production Injector	Experimental Injector
Maximum speed (m/s)	200–2000	200
Acceleration (g)	1500	5000
Repetition rate (Hz)	6	Single shot, room temp. burst mode
Target type	Direct/indirect	Direct/indirect
Target mass (gm)		
Direct target	0.015	0.015
Indirect target	1	1
Diameter direct target (mm)	5	5
Cylinder diameter indirect target (mm)	8–15	12
Sabot dimensions	TBD	TBD, diameter 2 mm greater than cylindrical target
Sabot materials	TBD	TBD
Sabot/pellet separation approach	TBD	TBD
Target exit accuracy (milliradians)	± 1	± 1
Cryogenic fuel temperature rise (from start to shot, K)	0.5	0.5
Availability	>99%	Several shots per day

requirement is to increase injection speeds up to ~2 km/s (at least for direct drive targets) in order to keep the temperature rise to acceptable levels. With the exception of the first three items, it is seen that no differences are expected between the production and experimental injectors. That is, the experiment must very closely simulate all the expected acceleration loads on targets. The targets have the same dimensions and mass as actual targets. The experimental injector must demonstrate the ability to produce accelerations (that can result in 2 km/s speeds in the production injector) with an acceptable test system length. A minimum exit velocity of 200 m/s has been selected for concept demonstration purposes. In order to achieve this velocity with the experimental accelerator, a higher acceleration capability than is needed in the production unit will be required. This will also enable us to seek the limits of acceleration, tracking, repeatability, and temperature. The injection process must not heat the target above a given temperature. The total allowable temperature rise of ~0.5 K from start to the point of the reaction is small. This includes travel through the acceleration, sabot separation, tracking, and chamber sections. The target must also exit the accelerator with an accuracy that will place it with a given accuracy at the chamber reaction site (± 0.4 mm position error for indirect drive, and ± 0.02 –0.2 mm for direct drive, personal communication, R. Petzoldt).

Most of the target related injection can be demonstrated with a short single shot test accelerator that is capable of accelerations that exceed those required for a reactor. Also desirable for a test facility is the ability to produce room temperature “burst mode” operation, where a pellet “bunch” (on the order of five per bunch) is injected in a single burst into the test chamber. This will allow experiments to be carried out involving the target tracking. The sabot spin rate is not well defined and will likely depend on whether the spin is used to separate the sabot from the target or to stabilize it during flight. The spin rate required for one may be different than the other.

The intent is to identify required sabot characteristics that will enable us to design an injector system that is capable of meeting the other requirements listed in the table.

M.3. Available Accelerator Options

In order to characterize the many options for injecting pellets into ICF reactors, a “family tree” of accelerators for direct drive and indirect drive targets was developed. These are shown in Figs. M-1 and M-2.

Fig. M-1. “Family Tree” of direct drive target accelerator concepts.

(1) Low speed electrostatic levitation and acceleration of a non-contacting sabot (that satisfies voltage criteria) may be possible.

(2) High speed contacting would be feasible only if the maximum temperature rise is acceptable.

(3) The target case must be able to withstand the heat and have no high Z material issues.

Fig. M-2. "Family Tree" of indirect drive target accelerator concepts.

There are probably a number of ways of categorizing the accelerator concepts. We have used the following logic. For each of the target types (direct and indirect), the acceleration can be accomplished with or without a sabot, at "low" or "high" speed, and can use either a contacting or non-contacting sabot. The definition of high speed is taken to be on the order of 2,000 meters per second. "Low" speed is defined as speeds currently achieved with gas guns tested with IFE in mind: less than ~200 m/s.

Both direct and indirect drive targets that use a sabot and are limited to low speeds have the most options for injection. If contacting, injection can be achieved using gas guns (which have been demonstrated far beyond the other concepts), as well as using rail gun and magnetic drive. The advantages of rail guns or magnetic drive over gas guns is unclear at low speeds. Non-contacting (low speed, with sabot) is only feasible with either electrostatic or magnetic accelerators. For systems requiring high speed, with sabot, and are contacting, voltage limitations allow only the rail gun or magnetic concepts to be considered. Gas guns may also be feasible for relatively high speeds. However, they will likely have problems associated with heating, wear on the sabot, the use of significant amounts of gas when used in a repetitive mode, and the size of the equipment used. Non-contacting, high speed, with sabot systems are most credible with a magnetically driven accelerator. While a non-contacting electrostatic accelerator can be envisioned with a sabot, it is unclear how the charge would distribute itself on the sabot, and how this would affect the dynamic stability of the motion. If systems without a sabot are considered, indirect drive potentially offers more options for acceleration, since the protective target case can be used to place components such as conducting, or magnetic

materials, as one would do with a sabot. Direct drive systems, that use no sabot are accelerating the pellet directly. Therefore, this limits the options to using electrostatic or magnetic means.

While these figures are qualitative, they are instructive. The basic conclusion is that if we would like to design a test accelerator that can achieve high levels of acceleration, within a reasonable length, for both direct and indirect targets, the most promising option appears to be magnetic means. Electrostatic acceleration is also worth investigating to understand the limits for the target parameters listed in Section M.2, Table M-1.

M.4. Promising High Speed Options

From Fig. M-1 and M-2, it is seen that if the desire is to design an accelerator that can achieve high speeds for both direct and indirect targets, the approach that appears promising is a magnetic drive system. However, since electrostatic acceleration is potentially very attractive for direct targets, without a sabot, we felt that it was worth investigating its limitations. In the following two sub-sections, the acceleration limitations of both the electrostatic and magnetic accelerator concepts will be discussed.

M.4.1. Electrostatic Acceleration Limits

We have qualitatively investigated the acceleration of direct drive targets, with no sabot using electrostatic acceleration. The details of the analysis are presented in a Mathcad worksheet in Attachment 1. Only the conclusions are presented here. Electrostatic acceleration is achieved by applying an electric field on a charged direct drive target (i.e. pellet). The achievable acceleration, and hence the required length of accelerator to reach a given final speed, is dependent on the maximum electric field that the pellet can withstand. A range of $0.5\text{--}3 \times 10^7$ V/m is approximately the breakdown stress level for vacuum, and lower if there is any background gas ("Microwave Tubes", by A.S. Gilmour, Jr., Bookcrafters, Inc., Chelsea, MI, page 438). A value of 1.35×10^7 V/m is obtained using Kilpatrick's criterion. This empirical condition is for avoidance of vacuum sparking. If one uses a value one tenth of that predicted by this criterion, or about 10^6 V/m, the force on a 0.015 gram pellet (Table M-1 requirement) that can be achieved is 0.012 N, an acceleration of 800 m/s^2 (or about 80 g's), and final speed of 400 m/s for a 100 m long accelerator. To reach 2,000 m/s at this acceleration then requires an accelerator of length $L = v^2/2a = (2,000)^2 / 2(800) = 2,500 \text{ m}$. This is clearly impractical. This does not preclude the possibility that a circular accelerator that makes multiple passes can be made to work. The inherent simplicity of a linear system is then lost, and the centrifugal acceleration will impose additional constraints. It also remains to be analyzed in detail just what the voltage limits on the pellet/accelerator system are; it could well be that the voltage requirements may be more restrictive. Finally, indirect drive targets, or heavier targets are probably excluded from using electrostatic acceleration.

M.4.2. Magnetic Acceleration Limits

In this sub-section, a qualitative assessment of the acceleration limits of a magnetic system is described. Instead of a detailed calculation, one can simply perform a magnetic pressure analysis to investigate the order-of-magnitude requirements on the magnetic field strength for a given level of acceleration on a given mass target. The magnetic pressure on a high conductivity conductor is given by:

$$P_m = B^2 / 2 \mu_0 ,$$

Where B is the magnetic flux density, and μ_0 is the permeability of free space. This pressure acts on the conducting section of the target to produce the accelerating force. The level of acceleration required is a function of the exit speed, and the length of the accelerator. Assuming a final speed v of 2,000 m/s, and a length L of 27 m, results in a required acceleration of:

$$\text{Acc.} = v^2 / 2 L = (2,000)^2 / 2 (27) = 74,000 \text{ m/s}^2 = 7,560 \text{ g's} .$$

The required magnetic flux density can be estimated from the force that is needed to give this acceleration. For a 1 gm, 1 cm diameter target, the force is given by:

$$F = ma = (0.001 \text{ kg}) (74,000 \text{ m/s}^2) = 74 \text{ N} .$$

The pressure is then $F/A = 74 \text{ N} / \pi (0.005)^2 = 9.4 \times 10^3 \text{ N/m}^2$ (Pa). The magnetic flux needed for this pressure can be calculated as:

$$B = \{2 \mu_0 P_m\}^{1/2} = 1.54 \text{ Tesla} .$$

This is a rather modest field, especially for the rather small acceleration tube area over which this field is needed. The conclusion is that magnetic fields appear to be a promising approach to accelerate targets of this size and mass in reasonable lengths.

M.5. Magnetic Accelerator Variations

Magnetic acceleration of both direct and indirect targets can, in the most basic terms, be thought of as a magnetic field acting on the dipole moment that is set up on the sabot. The variations among the different concepts has to do with how this dipole moment is set up on the sabot. Taken in this wide context, there are two basic classes of magnetic accelerators: induction accelerators, and "passive" accelerators. We define an induction accelerator to be an accelerator that directly induces eddy currents on a conducting sheet that is part of the sabot, which then interacts with the applied magnetic field to produce a force. A "passive" accelerator is defined as one which uses a sabot that has a dipole moment induced on it, without requiring a changing background field. In the following sections, we summarize these concepts.

M.5.1. Basic Approach

We have identified one induction and three “passive” accelerator approaches. These are characterized by the element on the sabot that induces the required dipole moment. The approaches are:

M.5.1.1. Induction Accelerator. This consists of a conducting cylindrical sheet of material that is part of the sabot as shown in Fig. M-3. The changing background field that is generated by the solenoid magnets induces a circulating eddy current on the conducting material (most likely candidates are Al and Cu). This induced current interacts with the applied changing field to accelerate the sabot. The coils can also be connected in a multi-phase configuration to produce an axially travelling magnetic field. In this case, this is really a simple linear induction motor, with the sabot acting as the armature. The concern with this approach is the heating that is generated by the induced eddy currents needed to produce the high accelerations required. Calculations we have performed (to be discussed in Section M.6) show that induced currents on the order of kAmps are needed to produce the levels of required acceleration on the sabot). For a copper conducting sheet placed on the sabot, these induced eddy currents result in a temperature rise of about 1,000 K, in the time it takes the target to travel through the accelerator. This is clearly too high. Hence, an induction accelerator is not suitable for this application.

Fig. M-3. Currents induced on conducting sheets produce acceleration.

M.5.1.2. “Iron Insert” Sabot Accelerator. In this passive approach an iron cylindrical insert is put into the sabot as shown in Fig. M-4. The geometric configuration is unchanged, except that the coils are operated in a different manner. Since the Fe will always be attracted to the coils, acceleration is obtained by having all the coils ahead of the sabot on, and the coils behind it turned off, as the target travels through the accelerator. The applied magnetic field magnetizes the iron, which is well characterized by the B-H curve of the iron used. This magnetization (which results in a dipole moment oriented in the axial direction) interacts with the magnets in front of the sabot, thus pulling it forward at a very constant acceleration, since the sabot will really only interact with a given

number of coils just ahead of it. The coils would be turned on (ahead of the pellet) when starting out, and be sequentially turned off as the sabot passes by. The advantages of this mode of acceleration are that the heating on the Fe will be much less than the conducting sheet approach (although there will still be some ac and hysteresis heating), and the operation of the coils may be simpler. There will likely be some quality control issues on the iron characteristics, although the acceleration may turn out to be fairly insensitive to this. This will need further study. This approach is not magnetically stable against radial motions. That is, if the sabot was non-contacting, a radial displacement would not produce a centering force; the sabot would be attracted to the magnets, and hence to the side wall. However, this is clearly a feasible approach if a contacting sabot is used. This becomes a good magnetic option therefore for low speeds. The level of acceleration achievable within the limits of the iron saturation needs to be evaluated.

Fig. M-4. Magnetization induced on the iron insert results in attraction to the coils ahead of it to produce uniform acceleration.

M.5.1.3. “Permanent Magnet” Insert Accelerator. In this approach, we replace the iron insert discussed above with a high field permanent magnet that produces the required dipole moment. There are available off-the-shelf very high field permanent magnets that can be considered, such as Neodymium Iron Boron (NdFeB), or Samarium Cobalt (SmCo). The potential advantage of this approach is that higher moments may be possible than the iron insert approach, with higher resultant accelerations. The potential problem with this approach is that these magnets can be de-magnetized if a repulsive field is applied to them. This means that they must operate in an attractive mode as done with the iron. However, it is not known how fringe fields for example will affect the magnetization. In addition, it is not known how sensitive these permanent magnets will be to the high levels of acceleration that will be generated. This approach will also be contacting, since it is not stable against radial sabot displacements.

M.5.1.4. “High Tc” Insert Accelerator. Another variation is to use a high temperature superconducting insert in the sabot. This is potentially attractive because of how well matched the operation temperature of the sabot is to these materials. The critical temperatures are in the 100 K range for high Tc materials; hence, they have very high

achievable current densities in the 20 K range (the colder the better). This means that a high T_c insert or perhaps a layer on the outside of the sabot may be sufficient to produce the required magnetic dipole moment. Of course some thought needs to be given to the process of how the material will be put in the magnetized state, but this may not be too difficult since the pellet/sabot system will need to be handled with cryogenic equipment anyway. This may turn out to be a very simple and passive non-contacting approach, but would require cold target testing to demonstrate. Because it is a repulsive system, it will have radial restoring forces, and can be made with a non-contacting sabot. The level of restoring force, and the achievable level of axial acceleration will be evaluated in the following section.

To summarize, the greatest chances for success appears to be the “Iron Insert” approach for “low speed” contacting applications, and a high T_c approach for the non-contacting “high speed” approach. The induction accelerator approach has much too high heating in the conducting element. The permanent magnet approach may have problems associated with de-magnetization, and its ability to withstand high levels of acceleration without losing its magnetic properties.

M.6. Magnetics Analysis

In this section, we discuss the electromagnetics analysis performed for the low speed iron insert approach, and the high speed High T_c approach. The analysis was performed using the Vectorfields software OPERA-2D, which is a finite-element code capable of performing two-dimensional, non-linear, transient calculations. There is a three-dimensional version, called OPERA-3D, which will be used for future more detailed calculations, as needed. All of the nominal position (that is axially centered) calculations have been performed using an axi-symmetric model. All radially displaced calculations are two-dimensional. Below, we discuss the two magnetic options (low speed and high speed).

M.6.1. “Low Speed” Iron Insert Approach

A two-dimensional non-linear finite-element, steady state model of the acceleration coils and the iron insert was developed. The model is shown at two stages of motion in Figs. M- 5 and M-6. The iron insert is assumed to be a cylinder with a mass of 3 grams. The coils are solenoids each carrying a total current of 24 kA-turns. The central field in the acceleration tube is 1.4 T. The iron insert moves up towards the coils. The coils are turned on ahead of the iron. The field from the coils magnetizes the iron (as high as the saturation level of the iron will allow). The magnetic field lines can be seen entering the iron in these figures. The calculations assumed a typical steel B-H curve. The induced field in the iron then interacts with the field in the coils, which result in the iron being attracted to the coils ahead of it. Figure M-5 shows the iron about to enter the coils. The coils adjacent and behind the iron have been turned off. Figure M-6 shows the iron about

halfway into the coil. The coil will be turned off before the iron insert reaches the midpoint of the coil, since the coil regions behind the iron will tend to slow it down. The force is a function of position, becoming weaker as the sabot enters the coil ahead of it.

The average force was calculated by moving the iron insert to different axial positions taking the average value (averaged over z). The result is an average force of about 30 Newtons. This results in an acceleration on a 3 gram iron piece of about 1,000 g's. Hence, a speed of 200 m/s is reached in a distance of $L = v^2/2a = 2$ m. This is a very short distance, and provides a potentially good solution for low speed injection. It should be noted that this is of course a contacting system, and issues of sabot wear and cycling need to be addressed. Nevertheless, it is an alternative to using gas guns for low speeds.

Fig. M-5. Axisymmetric, nonlinear magnetics model of iron insert about to enter magnet ahead of it. Force is maximum near this location.

M.6.2. "High Speed" Tc Approach

An ac (alternating current), two-dimensional finite-element model of the acceleration coils and the high temperature superconducting sabot material was developed for the purposes of: (1) calculating the induced "supercurrent" on the high Tc superconductor, (2) calculating the force as a function of position of the sabot with respect to the acceleration coils, and (3) calculating the amount of restoring force in the radial direction if the sabot is not perfectly centered. Figures M-7 and M-8 show the basic geometry of the model. For the purposes of the analysis, a cylinder of high Tc material is assumed. Calculations were performed for different length of superconductor to investigate the

Fig. M-6. Magnetics model of iron insert halfway into the magnet ahead of it. Force is decreasing at this location. Coil will be turned off soon.

amount of achievable magnetic force. Figures M-7 and M-8 show high T_c cylinder lengths of 2 and 3 cm, respectively. The current in the acceleration coils is again taken to be 24 kA peak (they operate in ac mode). The alternating magnetic field will set up a persistent current in the superconductor when first exposed to the field. For the purposes of the model, a fictitiously high electrical conductivity was assumed to calculate the induced current, with a coil current frequency of 10 Hz. It should be noted that this is not the actual frequency that the coils would operate under, but a frequency used to calculate the induced currents in the model. Since we are using a superconductor, the level of the induced current is not frequency dependent. As long as the skin depth is significantly less than the thickness of the cylinder, the model will be valid. The current level in the superconductor is about 8 kA. The field generated by the coils interacts with the dipole moment of the high T_c material to produce a repulsive force. The coil adjacent to the sabot would be turned on as the center of the sabot passed the mid-point of the coil.

We also investigated the radial restoring force on the sabot if it is off-center by a given distance. These calculations cannot be performed using an axi-symmetric model, and a two-dimensional geometry was used. Past experience shows that this is certainly adequate for preliminary calculations. The model used is shown in Fig. M-9 and M-10, at different stages of the motion. The figures show a displacement (to the right) of 0.2 mm. The calculations were also performed for a displacement of 0.1 mm. The level of the restoring force is dependent on the position of the sabot with respect to the coils. When the sabot (actually the high T_c cylinder) is at the axial center of the coil and displaced 0.2 mm to the right, as seen in FigM-9, the restoring force pushing it back to center is 4.6 Newtons. At the position shown in Fig. M-10, the restoring force is lower:

Fig. M-7. The induced persistent current on the high Tc material interacts with the field of the coils behind it to produce a repulsive force (pushed down in the figure). The figure shows a 2 cm long high Tc cylinder.

Fig. M-8. A 3 cm long high Tc cylinder being pushed forward by the coils behind it.

about 3 Newtons. These numbers represent about 10% of the axial acceleration force, which is significant. For a 3 gram target mass, this results in a radial restoring acceleration of greater than 100 g's. Magnetically, the target is stable in that the restoring forces tend to bring the sabot to the axial center. The amount of uncertainty has to do with the damping forces that act on the sabot, and the actual dynamic motion during its flight through the accelerator. The damping forces will be due to any currents that are induced in the system, including eddy currents on the accelerator tube, the high Tc

Fig. M-9. A 2 cm long high Tc cylinder is displaced radially by 0.2 mm, compressing the magnetic field lines, hence providing a restoring force.

Fig. M-10. The high Tc cylinder is displaced radially by 0.2 mm near the exit point from the acceleration coil.

material (and its matrix material), etc. It is possible that the dynamics of the sabot *during its flight in the accelerator* need not be precise as long as it doesn't make any physical contact. Final focussing before the sabot releases the target into the reaction chamber may be possible with, for example, a pair of quadrupole magnets. This is clearly an area

requiring considerably more analysis and study.

The achieved force on the high Tc cylinder was found to be an average of 37 Newtons. For a 3 gram target, this results in 1,260 g's of acceleration. Hence, a speed of 2 km/s is reached in a distance of about 160 m. This is considered a reasonable distance in line with sizes assumed in IFE reactor studies. It would be desirable to increase the current in the coils to decrease this distance. The ability to do this depends on not exceeding the critical field of the superconductor. This should be possible, since the critical fields for these materials are very high (on the order of 100 T).

We also performed force calculations by assuming a constant induced supercurrent, and calculated the force of a solenoid acting on it as a function of position using an analytical formulation. This calculation is shown as a Mathcad document in Attachment 2. The formulation assumes a constant current on the cylinder of value I . The resulting magnetic moment is then given by $\mathbf{m} = IA \mathbf{e}_z$, where A is the area of the current loop, and $\mathbf{e}_z$ is the unit vector in the z direction. The force acting on the moment in the z direction is then given by: $F_z = m_z dB_z/dz$. The magnetic field is described as a simple expansion around the center point ($z=0$) of the solenoid. The basic conclusion is that the force on the superconducting cylinder varies significantly as it is being acted upon by the solenoids. This can be made much smoother by sizing the coils to be smaller than the length of the high Tc cylinder. These calculations also produce the same acceleration force used on the more accurate OPERA-2D calculations.

M.7. State of High Tc Superconductors

M.7.1. Specifications

Based on the preliminary magnetics calculations performed in the previous section, we can evaluate whether the present state-of-the-art of high temperature superconductors can satisfy the required current density level that the induced supercurrent requires. We require a superconductor that has approximately the following specifications:

Outer Diameter (cm)	1
Inner Diameter (cm)	0.5
Length (cm)	1.5
Configuration	Solenoid
Operating Temperature (K)	19
Desired Ampere Turns NI (A)	10,000
Desired Operating Field (T)	1.5

M.7.2. Superconductor Options

Since the coil is operating at 19 K, it is logical to consider the application of the following types of superconducting materials:

1. Bi-2223 Ag-based HTS tapes
2. IBAD Y-123 Ni-based conductor
3. Y-123 melt textured permanent superconducting magnet
4. Nb₃Sn conductor

The goal of this preliminary investigation was to see if any of the above materials are currently ready for this type of application. Below, we consider these options.

M.7.2.1. Bi-2223 Ag-Based HTS Tapes. This is the most commonly available HTS conductor today. At least three commercial companies are producing over 100 km each per year and it has been applied to power utility applications like fault current limiters, transformers, transmission lines and motors. Its disadvantages are its inability to maintain a high critical current density at high field and high temperature (e.g. at LN₂ temperature of 77 K, though its critical temperature is around 110 K) and its high cost (due to having a silver substrate). The following table summarizes its performance (critical current density, J_{sc}) as a function of field* and temperature (Ref. M-1):

Operating Field (T)	J_{sc} at 77 K (A/cm ²)	J_{sc} at 20 K (A/cm ²)
0	17,000	105,000
0.5	1,000	60,000
1.0	300	48,000
2.5	200	35,000

A typical Bi-2223 conductor of this type will carry 21.5 A at 77 K and zero magnetic field and it measures 3.5 mm × 0.22 mm. Conductors with this kind of performance can be produced at unit lengths exceeding 1 km each. Conductors with smaller unit lengths can have performance as much as three times the above figure. Let's assume that we can have a conductor that can carry 60 A at 77 K and 0 T, with the same cross section area cited above. Then by operating at 20 K, the current that can be carried in each tape is increased significantly to 35000/17000*60 A or 121.5 A. With a cross section of 1.5 cm × 0.25 cm and an assumed packing factor of 80%, we can carry 4 × 9 × 121.5 A or 4447 Ampere turns, short of the required 10,000 Ampere turns by about a factor of two. In addition, the maximum bending strain incurred would be 0.11/5 or 2.2%. There would also be a significant degradation in the current carrying capability of the Bi-2223 conduc-

*Field perpendicular to the direction of current flow in the conductor.

tor at this strain level. It is therefore concluded that the most common type of commercially available HTS conductor would probably not be good enough for our application, at least at the present time.

M.7.2.2. IBAD Y-123 Ni-Based Conductor. For the IBAD Y-123 Ni-based conductor, the expected performance is as follows:

Operating Field (T)	J_{sc} at 75 K (Refs. M-2, M-3) (A/cm ²)	J_{sc} at 4 K (Refs. M-2, M-3) (A/cm ²)	J_{sc} at 20 K (Refs. M-2, M-3) (A/cm ²)
0	1,200,000	10,000,000	6,000,000
0.5	400,000	6,000,000	4,300,000
1.0	240,000	3,800,000	2,900,000
1.5	200,000	3,000,000	2,600,000

This table shows the expected change of critical current density with magnetic field in the worst direction (the direction which degrades the current carrying capability most).

The latest test results from LANL on their conductor shows a 96 A current carrying capacity from a 0.05 mm $\times$ 1 cm conductor at 75 K and self field. Notice that the degradation from 0 T (self field to 1.5 T) is only a factor of $1,200,000/200,000 = 6$ compared to a factor of $17,000/200 = 85$ for the case of the Bi-2223 conductor. The same coil outlined above can now carry $96/6 * 40 = 640$ A-turns or double to 1280 A-turns if we allow the coil to grow to 2 cm long. Operating at 20 K would yield a total ampere-turns of 8320 or 16,640 respectively. If a tape with width of 0.75 cm can be manufactured, we can have two conductors wound parallel in the coil and we would get a total ampere-turns of 12,480. This would meet the requirement above.

A big advantage of this conductor is that strain degradation is not an issue here. It has been wound into very tight coil with a diameter of 0.25 cm and the critical current density shows only minimum degradation even with such a tight radius of winding. In addition, the nickel based substrate will be much more inexpensive compared to a silver based substrate.

Hence, we can conclude that if and when this nickel based IBAD conductor is available, it would meet our stated requirement. We are looking at between 2 to 3 years before this can be accomplished.

M.7.2.3. Y-123 Melt Textured Permanent Superconducting Magnet. Private communication with Dr. Yuki Iwasa of MIT, who pioneers in the using of YBCO permanent superconducting magnet indicated that a current density (Ref. M-5) of 10,000 A/cm² is easily achievable at 77 K and self field, for a YBCO disk fabricated in the form of the

coil we need. This would give us a total ampere turns of $0.25 \times 1.5 \times 10000 = 3750$ at 77 K and zero field. At 1.5 T and 20 K, it is estimated that we may be able to double it to 7,500. This is data obtained through private conversation, and scratchy at best at this point because this material is rare and only a few places in the world produce it. It is definitely worthwhile spending a little more time on this approach to see if this would meet our requirement.

M.7.2.4. Nb₃Sn Conductor. The critical temperature of Nb₃Sn conductor at 1.5 T is at 17.5 K, less than the 19 K operating temperature listed in the specification, so this is not a viable option.

M.7.3. Summary

Of the four options listed above, the IBAD conductor approach should be viable in 2 to 3 years. It is probably worthwhile checking into the superconducting permanent magnet approach a little more. Unfortunately, the commercially available Bi-2223 won't be viable in our prescribed operating conditions. And finally, Nb₃Sn is a poor choice at 19 K.

M.8. Conceptual Layout of a Production Low Speed Magnetic Accelerator

Since our goal is ultimately to design an injection system that will be used for a production IFE facility, it is important to ensure that the acceleration concepts being considered are compatible with this. To this end, we have done a very preliminary conceptual design of a target accelerator system. By system, we mean that in addition to accelerating the target, we need to understand how the target is released from the sabot, how the sabot is recycled, loaded with a fresh target and launched again. To help understand the issues from a system point better, we created a model using the design package Pro-Engineer. An overall view of the system is seen in Fig. M-11.

This is the low speed contacting system, that uses an iron insert in the sabot. The overall length is about 2 m. The acceleration region is all we have been discussing so far. The sabot/target system is accelerated to the desired final speed (about 200m/s); the deceleration coils decelerate the sabot, releasing the target into the reaction chamber. The sabot is diverted away from the flight path of the target, either mechanically or using divertor coils. The empty sabots then go to the "sabot automated loading rack", which basically serves as the sabot inventory module, and loads a fresh batch of empty sabots, sending them up to the target loader. The empty sabots are loaded with targets at the loader, and then sent to the injection area to begin the process over again.

Fig. M-11. Conceptual layout of target accelerator system.

More detailed views of the components in this system are shown in the following figures. Figure M-12 shows the injection region. The sabot has been previously loaded with the target, and is ready for the acceleration stage. Also visible in this figure is the "screw" that is part of the sabot, responsible for placing a spin on the target, very similar to that in a rifle. The screw is of course also the contacting element. The iron insert is not visible, but it is a cylindrical element contained within the sabot. The target is assumed to be a direct drive target (pellet) that is carried in the front of the sabot. Figure M-13 shows the target separation region. After the sabot is decelerated (by an amount that needs to be determined), the target separates itself from the sabot and continues at the final top speed into the chamber. The sabot is diverted magnetically in Fig. M-13 by using a coil that is placed at an angle with respect to the path of the target. The empty sabots are magnetically pulled into the "sabot automated loading rack", shown in Fig. M-14. This loading rack also provides the sabot inventory. It consists of linear groups of empty sabots ready to be loaded. The loader moves in the plane perpendicular to the linear sabot array in a pre-programmed manner so that sabots are re-used at a pre-determined rate. Upon loading into the next section, the empty sabots are pulled up to the loading area (Fig. M-15), where the targets are loaded. Of course, this is all done under cryogenic conditions.

Fig. M-12. Sabot with target ready for injection.

Fig. M-13. Target is separated from sabot.

Fig. M-14. Sabots are recovered and loaded.

Fig. M-15. Targets are loaded on sabots.

M.9. Other Technical Issues

Some of the other supporting calculations are presented in this section. This includes preliminary discussions of the coil design, other magnetic options, spin stabilization, and damping induced heating.

M.9.1. Coil Design

The baseline coil configuration used in the magnetics analysis is shown in Fig. M-16. It has 24 kA-turns. This produces a central field of about 1.5 T. The actual coil configuration will be determined by the achievable current density in the copper coils, and the accelerator issues, such as the smoothness of the force delivery, and the position the accelerator (coils may be longer in the faster stages). Pulsed coils have in the past also used simple single turn coils as shown in Fig. M-17. These are usually made of stainless steel or BeCu. Other driving factors may include the type of power used (e.g. capacitors, inverters, rotating machinery).

Fig. M-16. Baseline coil configuration.

Fig. M-17. Single turn coil option.

M.9.2. Other Magnetic Options

While a solenoid accelerator/sabot configuration is attractive, it is unclear what the issues with dynamic stability of the target will be. It is therefore worthwhile to have other magnetic options in mind. An example of an alternate potential configuration is shown in Fig. M-18. It is based on a gun concept developed in the 1980s by Sandia (Bill Cowan was the project leader). It consists of rectangular coils above and below an aluminum plate target. Pulsed current in the coils produces eddy currents in the plate that repel it forward. The magnetic field lines “reconnect” behind the plate as it goes past. A variation on the reconnection gun concept for our high speed accelerator can be configured by placing “wings” on the sabot which are made of high temperature superconductor. The high T_c material may actually be tape that could not in the previous design be wound in a solenoid geometry. The flat configuration may also offer advantages as far as providing greater restoring forces. And finally, the sabot could easily be guided away once it had released its target. Magnetically induced spin would not be possible in this configuration.

Fig. M-18. Modified “reconnection gun” concept applied to target injection.

M.9.3. Spin Stabilization

The purpose of putting a spin on the target is to provide some dynamic stability as it is moving through a non-vacuum environment in the reaction chamber. While this is easier to do at lower speeds, on the order of 200 m/s, it is limited by the resulting centrifugal acceleration on the target. The faster the target is moving, the greater the frequency of spin must be in order that enough revolutions have occurred in traversing the approximately 5 m of distance from the point of accelerator exit to the reaction point. If we limit the acceleration on the target to 5,000 g's, the rotational speed that results on a 5 mm diameter pellet is about 16 m/s. For a 5 mm radius pellet this is about 500 Hz. In

the time it takes the pellet to traverse the 5 m (2.5 ms at 2,000 m/s speed), the pellet undergoes only 1.25 revolutions. It needs to be determined whether this amount of spin is useful. Of course, this issue is independent of the acceleration concept, and is an issue that is driven by the high speed. Magnetic concepts may allow for an accurate enough final focussing to make spin unnecessary. This final focussing can be achieved a number of ways. For example, a strong solenoid at the deceleration stage, or a pair of quadrupole coils (90 degrees apart) at the accelerator exit are possibilities.

M.9.4. Damping Induced Heating

A preliminary calculation was performed on the amount of damping induced heating that is possible given the amount of radial restoring force that was calculated in the magnetics analysis. The calculated restoring force was on the order of 4 N for a displacement of 0.2 mm. The energy that is dissipated by this force through this distance is then given by $E = Fd = 4 \text{ N} \times 0.2 \times 10^{-3} \text{ m} = 8 \times 10^{-4} \text{ Joules}$. Assuming a mass of the sabot that is 3 gm, and that the energy is absorbed in the high T_c material (assumed to be silver matrix), and the sabot casing (assumed to be epoxy properties), the rise in the sabot temperature can be estimated. Using C_p (Ag at 18 K) = 0.0116 J/gmK, and C_p (epoxy at 18 K) = 0.064 J/gmK, gives a temperature rise of 0.023 K in the silver, and 0.0042 K in the epoxy. These are very encouragingly low numbers, and may indicate that passive damping of the sabot by induced eddy currents may be a means for controlling the flight of the sabot during the acceleration phase.

M.10. Summary and Recommendations

The goal of this IR&D project was to analyze the acceleration of both direct and indirect targets to speeds on the order of 2 km/s in lengths that are typically considered for IFE reactor concepts (~150 m). The limitations imposed were thermal (no more than 0.5 K temperature rise), and "structural" (~1,500 g's for a production accelerator). Only certain types of magnetic acceleration of targets appear as likely candidates to achieve these goals. The greatest discriminator between concepts is the very stringent temperature rise requirement. Concepts such as the induction or rail gun accelerators are quickly eliminated as candidates. Electrostatic acceleration is a possible option for direct drive targets, at least up to several hundred m/s speeds. However, given our goal of selecting acceleration options that are capable of handling both types of targets at speeds up to 2 km/s results in magnetic accelerators as being the best overall option.

We have performed preliminary analyses on a low speed ("lower risk"), and a high speed ("aggressive design") option. The conclusions from this study are given below:

1. The selected low speed approach uses a magnetically driven, contacting sabot with an iron insert of about 3 grams mass. The magnetic field strength used is 1.4 Tesla. Because a contacting sabot is used, the level of accuracy with which the

target leaves the accelerator should be comparable with gas guns. A non-linear magnetics model of solenoids acting on a cylindrical iron sabot insert was developed. An acceleration of about 1,000 g's is achieved, and 200 m/s is attained in only 2 m of accelerator length. This is an alternate (for low speeds) to the gas gun. It may have the advantages that the force delivery to the sabot/target is smoother and more controllable, and that there is no gas in the system.

2. A preliminary conceptual design of a low speed iron insert, magnetically driven accelerator system was developed using the design package Pro-Engineer. The sabot is contacting via a "screw" on its outside to produce spin. After acceleration, it is decelerated, releasing the target. The inventory of empty sabots is kept in a module that is envisioned to provide automated rapid loading of a batch of sabots, and can be replaced as a unit with a fresh inventory. The advantage of this system is its relative simplicity and compactness.
3. The selected high speed "aggressive design" accelerator uses a non-contacting, magnetically driven sabot that uses a passive high temperature superconductor. The high T_c material is either in the form of a "pellet" or a small radius winding unto the sabot. Using currently achievable, or near term achievable values of the critical current density, a modest accelerator field of 1.4 T can produce an acceleration of about 1,300 g's. A speed of 2,000 m/s is achieved in a length of 160 meters of accelerator, which is in line with dimensions assumed in existing reactor studies.
4. The radial stability of the target is very important for the non-contacting high speed system. The restoring force on the high T_c material was calculated as a function of radial displacement. The restoring forces are on the order of 10% of the axial acceleration force, which should be sufficient to maintain a non-contacting system. The level of damping needed to control the flight needs to be determined. One approach is to use the induced eddy currents when the target is off-center to produce the damping.
5. Focussing magnets can be used to provide final directional control on the target. These can be either in the form of solenoids, or perhaps a pair of quadrupole magnets 90 degrees apart.
6. The electrical conductivity of the DT pellet needs to be investigated. If significant heating of the pellet due to induced eddy currents can occur, then means to shield the pellet (using the high T_c material would work) need to be evaluated.
7. The behavior of the high T_c material under the changing magnetic field environment of the accelerator needs to be evaluated. Specifically, the penetration of the magnetic field into the superconductor, and the detailed current

distribution need to be studied. Also, the effects of high levels of acceleration (most easily done by testing on a shaker) should be investigated.

8. The power delivery system to the magnets will use technologies that are probably less demanding than those used for rail guns and co-axial accelerators in the 1980s. It needs to be determined what the best power options are. These can include capacitors (either connected individually to the coils, or connected to a bus network), inverters, or rotating machinery. The best means for producing acceleration needs to be a part of this study, whether as a series of pulses or a traveling wave.

M.11. References

- M-1 Bi-2223 Ag-based conductor performance data, through private communication with Intermagnetics General Corporation.
- M-2 S.R. Foltyn, et al., "High T_c Coated Conductors- Performance of Meter-Long YBCO/IBAD Flexible Tapes," submitted to IEEE Transactions on Applied Superconductivity (1998).
- M-3 Private conversation with Dean Peterson, Los Alamos National Laboratory.
- M-4 Extrapolated from data included in *Research and Development Roadmap to Achieve Electrical Wire Advancements from Superconducting Coatings*, edited by J.W. Muehehasuer, The University of Tennessee Space Institute (1997).
- M-5 Private conversation with Dr. Yuki Iwasa at MIT.

Attachment 1: Electrostatic Acceleration Limits

Given the radius of the pellet, first calculate the peak electric field on the surface of the pellet. Assume a surface charge on a 3 mm radius pellet of $q = 1.3 \times 10^{-8}$ Coulombs. This is taken from the paper by R. Petzoldt [Petzoldt and Moir, "Target Injection Methods," Fusion Technology **26** (1994)]. Let r be the pellet radius in mm, and Q be the charge density in units of Coulombs/mm².

$$r := 3 \text{ mm}$$

$$Q(r) := 1.3 \times 10^{-8} / 4 \cdot \pi \cdot r^2$$

$$Q(r) := 1.149 \times 10^{-10} \text{ Coulombs/mm}^2$$

The surface charge q on a pellet of a given radius R is then given by, for R in the range of 0.5 to 5 mm:

$$R := 0.5, 1..5 \text{ mm}$$

$$q(R) := Q(r) \cdot 4 \cdot \pi \cdot R^2$$

$q(R) =$
$3.611 \cdot 10^{-10}$
$1.444 \cdot 10^{-9}$
$3.25 \cdot 10^{-9}$
$5.778 \cdot 10^{-9}$
$9.028 \cdot 10^{-9}$
$1.3 \cdot 10^{-8}$
$1.769 \cdot 10^{-8}$
$2.311 \cdot 10^{-8}$
$2.925 \cdot 10^{-8}$
$3.611 \cdot 10^{-8}$

Given the charge on a pellet for a given pellet radius, the force on the pellet can be calculated by $F = qEa$, where Ea is the applied electric field. This applied E-field is obtained by applying a voltage difference across a given distance. Assume that the potential difference is obtained by successive charging of "rings" separated a distance "d" apart. The potential difference V is then given approximately by: $V = Ea \times d$. We can now estimate what kind of V it takes to produce a given acceleration. For a final projectile velocity "v", and an accelerator length L , the required acceleration "a" can be estimated. Assume the following numbers:

$v := 50, 100.500$ meters/s

$L := 100$ meters

Acceleration $a(v) := v^2/2 \cdot L \text{ m/s}^2$

$a(v) =$
12.5
50
112.5
200
312.5
450
612.5
800
$1 \cdot 10^3$
$1.3 \cdot 10^3$

The force required is given in Newtons as:

Assume a mass m of $m := 1.5 \times 10^{-5}$ kg (i.e., 0.015 gm)

Force(v) := $m \cdot a(v)$

Force(v) =

10^{-4}
10^{-4}
10^{-3}
10^{-3}
10^{-3}
10^{-3}
10^{-3}
0.012
0.015
0.019

The applied force is related to the voltage by the relationship $F = qE$, or $F = qV/d$, where as defined above V is the applied potential difference between rings, and d is their separation. The voltage required as a function of velocity is then given by:

$$d := 0.01$$

$$\text{voltage}(v) := \text{Force}(v) \cdot [d/q(e)]$$

Voltage (v) =

144.231
576.923
$1.298 \cdot 10^{-3}$
$2.308 \cdot 10^{-3}$
$3.606 \cdot 10^{-3}$
$5.192 \cdot 10^{-3}$
$7.067 \cdot 10^{-3}$
$9.231 \cdot 10^{-3}$
$1.168 \cdot 10^{-3}$
$1.442 \cdot 10^{-3}$

Voltage (v) =

This results approximately in the following applied electric stresses for different speeds:

$$E(v) := \text{voltage}(v)/d$$

E (v) =

$1.442 \cdot 10^4$
$5.769 \cdot 10^4$
$1.298 \cdot 10^5$
$2.308 \cdot 10^5$
$3.606 \cdot 10^5$
$5.192 \cdot 10^5$
$7.067 \cdot 10^5$
$9.231 \cdot 10^5$
$1.168 \cdot 10^6$
$1.442 \cdot 10^6$

E (v)

Therefore, for pellets of radius 3 mm and mass on the order of 0.015 gm, electrostatic accelerators require about 10^6 V/m E-field strength to produce accelerations on the order of 800 m/s^2 and a force of 0.012 N. This results in a final speed of $\sim 400 \text{ m/s}$ in a 100 m

long accelerator (for a 0.015 gm direct drive target). 10^6 V/m is probably as high as one should go to avoid breakdown, especially if there is any residual gas in the accelerator. This is also the value used by Petzoldt and Moir, "Target Injection Methods for Inertial Fusion Energy," *Fusion Technology* **26** (1994).

Attachment 2: Calculation of Field Due to a Finite Solenoid

Definition from Montgomery's book follow:

$$\begin{array}{lll} a1 := 0.6 \cdot 0.01 & a1 = 6 \cdot 10^{-3} & \text{Note that mks units are used, to convert from cgs} \\ a2 := 2.3 \cdot 0.01 & a2 = 0.023 & \text{used in Montgomery's book to mks, replace } 4\pi/10 \\ b2 := 3 \cdot 0.01 & b2 = 0.03 & \text{with } \mu_0. \end{array}$$

$$a := \frac{a2}{a1} \quad \mu_0 := 4 \cdot \pi \cdot 10^{-7}$$

$$a = 3.833 \quad \text{This is the value of "alpha"}$$

$$b := \frac{b2}{2 \cdot a1}$$

$$b = 2.5 \quad \text{This is the value of "beta"}$$

$$c1 := \frac{1}{1 + b^2} \quad c2 := \frac{b^2}{1 + b^2} \quad c3 := \frac{a^2}{a^2 + b^2} \quad c4 := \frac{b^2}{a^2 + b^2}$$

$$f := \mu_0 \cdot b \cdot \left(\operatorname{asinh} \left(\frac{a}{b} \right) - \operatorname{asinh} \left(\frac{1}{b} \right) \right)$$

$$e2 := \frac{1}{f} \cdot \mu_0 \cdot \frac{1}{2 \cdot b} \cdot \left(c1^{\frac{3}{2}} - c3^{\frac{3}{2}} \right) \quad e4 := \frac{1}{f} \cdot \mu_0 \cdot \frac{1}{24 \cdot b^3} \cdot \left[c1^{\frac{3}{2}} (2 + 3 \cdot c2 + 15 \cdot c2^2) - c3^{\frac{3}{2}} (2 + 3 \cdot c4 + 15 \cdot c4^2) \right]$$

$$:= \frac{1}{f} \cdot \mu_0 \cdot \frac{1}{240 \cdot b^5} \cdot \left[c1^{\frac{3}{2}} (8 + 12 \cdot c2 + 15 \cdot c2^2 - 70 \cdot c2^3 + 315 \cdot c2^4) - c3^{\frac{3}{2}} (8 + 12 \cdot c4 + 15 \cdot c4^2 - 70 \cdot c4^3 + 315 \cdot c4^4) \right]$$

$$:= \frac{1}{f} \cdot \mu_0 \cdot \frac{1}{896 \cdot b^7} \cdot \left[c1^{\frac{3}{2}} \cdot (16 + 24 \cdot c2 + 30 \cdot c2^2 + 35 \cdot c2^3 + 315 \cdot c2^4 - 2079 \cdot c2^5 + 3003 \cdot c2^6) \right. \\ \left. - c3^{\frac{3}{2}} \cdot (16 + 24 \cdot c4 + 30 \cdot c4^2 + 35 \cdot c4^3 + 315 \cdot c4^4 - 2079 \cdot c4^5 + 3003 \cdot c4^6) \right]$$

$H_0 := 2$ Field value (in tesla) at the center of the solenoid

$z := 0, 0.001 \dots 0.015$

$$H(z) := H_0 \cdot \left[1 + e2 \cdot \left(\frac{z}{a1} \right)^2 + e4 \cdot \left(\frac{z}{a1} \right)^4 + e6 \cdot \left(\frac{z}{a1} \right)^6 + e8 \cdot \left(\frac{z}{a1} \right)^8 \right]$$

$H(z) =$

2
1.997
1.988
1.974
1.953
1.925
1.891
1.85
1.801
1.744
1.678
1.603
1.518
1.424
1.32

Now find the force on a dipole of strength $m=A$ as a function of position:

$r_0 := 0.005$ radius of target (m)

$I_2 := 7500$ value of current in the target dipole

$$f(z) := H_0 \cdot \frac{I_2}{a1} \cdot \pi \cdot (r_0^2) \cdot \left[2 \cdot e2 \cdot \left(\frac{z}{a1} \right) + 4 \cdot e4 \cdot \left(\frac{z}{a1} \right)^3 + 6 \cdot e6 \cdot \left(\frac{z}{a1} \right)^5 + 8 \cdot e8 \cdot \left(\frac{z}{a1} \right)^7 \right]$$

$f(z) =$

0
-3.42
-6.888
-10.451
-14.156
-18.047
-22.168
-26.554
-31.234
-36.218
-41.496
-47.028
-52.727
-58.455
-63.995

$$\text{AveForce}(z) := \frac{\int_0^z f(z) dz}{z}$$

 $\text{AveForce}(z) =$

0
-1.708
-3.428
-5.172
-6.952
-8.778
-10.663
-12.616
-14.648
-16.765
-18.972
-21.269
-23.652
-26.11
-28.62

This is the average force in Newtons for the target as it travels between z and 0, where z is taken to be the center of the accelerating solenoid coil.

Appendix N: Cost Breakdown for Experimental Injection and Tracking System

**EXPERIMENTAL PLAN FOR IFE TARGET INJECTION
AND TRACKING DEMONSTRATION**

Project Staff

Injection System & Supporting Equipment Design				
Staffing	Heads	Months	Rate	Costs
Management	0.25	10	160	69280
Analysis	1	10	140	242480
Design/Test Engineer	1.25	10	115	248975
Designer	2	10	95	329080
Technician Support	0.15	10	95	24681
Other Support	0.5	10	100	86600
Capitol				
	5.15			1001096
Procurement & Fabrication				
Staffing	Heads	Months	Rate	Costs
Management	0.25	9	160	62352
Analysis	0.1	9	140	21823
Design/Test Engineer	1.35	9	115	242004
Designer	0.25	9	95	37022
Technician Support	1	9	95	148086
Other Support	0.25	9	100	38970
Capitol				400000
	3.2			950256
Assembly & Initial Shakedown				
Staffing	Heads	Months	Rate	Costs
Management	0.25	9	160	62352
Analysis	0.25	9	140	54558
Design/Test Engineer	1.5	9	130	303966
Designer	0.15	9	100	23382
Technician Support	2.25	9	100	350730
Other Support	1	9	100	155880
Capitol				
	5.4			950868
Acquire Data, Modify Equipment to Optimize Results				
Staffing	Heads	Months	Rate	Costs
Management	0.25	16	160	110848
Analysis	0.25	16	140	96992
Design/Test Engineer	1.5	16	115	478032
Designer		16	95	0
Technician Support	2	16	95	526528
Other Support	0.5	16	100	138560
Capitol				
	4.5			1350960

Initiate Design of Cryogenic Equipment					
Staffing	Heads	Months	Rate	Costs	
Management	0.25	4	160	27712	
Analysis	0.5	4	140	48496	
Design/Test Engineer	0.4	4	115	31869	
Designer	1.9	4	95	125050	
Technician Support	0	4	95	0	
Other Support	0.25	4	100	17320	
Capitol					
				250447	
Document Results/Support IRE Decision					
Staffing	Heads	Months	Rate	Costs	
Management	0.4	3	160	33254	
Analysis	0.5	3	140	36372	
Design/Test Engineer	1	3	115	59754	
Designer	0	3	95	0	
Technician Support	0	3	95	0	
Other Support	0.25	3	100	12990	
Capitol					
				142370	
Evaluate Magnetic Injector System Backup					
Staffing	Heads	Months	Rate	Costs	
Management	0.05	36	160	49882	
Analysis	0.5	10	140	121240	
Design/Test Engineer	0.2	10	115	39836	
Designer	0.5	8	95	65816	
Technician Support	0.5	12	95	98724	
Other Support	0.1	36	100	62352	
Capitol				69000	
				506850	
Note: Values and rates above incorporate travel, expendible materials, and other miscellaneous costs					
TOTAL COSTS -->					
				5152848	

CAPITOL EQUIPMENT LIST					
	Cost Est \$				
Precision Gun Barrel	20000				
Feeder Turret and Target Handling System	20000				
Tracking Lasers, Mounting Systems, & Detector System	40000				
Real Time Computer Control & Data Acquisition System	45000				
High Speed Camera System	25000				
Custom Software Programming & Circuit Boards	25000				
Computer Controlled Variable Gas Pressure System	40000				
Vacuum System, Instrumentation, Differential Pumping, & Injection Gas Handling System	110000				
Target Steering System	40000				
Vacuum Vessels	35000				
TOTALS -->	400000				